

Article

Energy and Exergy Potential of a Flow-Controlled Photovoltaic–Thermal Collector for Charging Thermochemical Energy Storage Under Intermittent Tropical Irradiance

Choosak Rittiphet ^{1,2}, Suratsavadee Koonlaboon Korkua ^{1,2}, Krit Funsian ¹,
Mohammad Faridun Naim bin Tajuddin ³, Santanu Kumar Dash ⁴ and Kamon Thinsurat ^{1,2,5,*}

¹ School of Engineering and Technology, Walailak University, Nakhon Si Thammarat 80160, Thailand; choosak.ri@wu.ac.th (C.R.); ksuratsa@wu.ac.th (S.K.K.); krit.fu@wu.ac.th (K.F.)

² Center of Excellence in Wood and Biomaterials, Walailak University, Nakhon Si Thammarat 80160, Thailand

³ Faculty of Electrical Engineering & Technology, Universiti Malaysia Perlis (UniMAP), 02600 Arau, Perlis, Malaysia; faridun@unimap.edu.my

⁴ TIFAC-CORE Vellore Institute of Technology, Vellore 632014, Tamil Nadu, India; santanukumar.dash@vit.ac.in

⁵ Center of Excellence in Sustainable Disaster Management, Walailak University, Nakhon Si Thammarat 80160, Thailand

* Correspondence: kamon.th@wu.ac.th

Abstract

Photovoltaic–thermal (PVT) collectors co-generate electricity and heat—natural front ends for thermochemical energy storage (TCES)—provided the heat transfer fluid stays above the reactor’s desorption temperature. Using an eight-node model of a 0.6834 m² collector at 8.64° N whose thermal core is partially validated against measured data from the same tropical–coastal site (rooftop PV module temperature, RMSE 3.8 °C; prototype absorber-to-water heat transfer, RMSE 1.3 °C), flow-regulated to the ≈95 °C SrCl₂/NH₃ desorption threshold, we quantify the energy and exergy delivered for charging under tropical–monsoon intermittency. The 95 °C setpoint operation, the ≈5.3 h charging window, and all reported exergy yields are simulated: the built prototype delivered hot water peaking at 79 °C and did not reach the 95 °C setpoint. On a measured clear-sky day (clearness index $K_t = 0.52$), the collector yields 1.38 kWh of energy but only 0.43 kWh of exergy (first-law efficiency ≈ 38%; gross exergy efficiency ≈ 13%); across a 30-seed synthetic-intermittency ensemble, the exergy yield is 0.678 kWh at ≈14% gross exergy efficiency (≈52% combined first-law efficiency). In both cases, the thermal stream dominates the energy output while the electrical stream dominates the exergy output—on the sunlit day, the exergy is about 80% electrical—because 95 °C heat carries a Carnot factor (exergetic quality factor, $1 - T_a/T_7$, at the instantaneous ambient dead state) of only ≈0.18 and an integrated Bejan/Kotas thermal-exergy quality of only ≈0.09. The controller holds the outlet within 1.4 K of the setpoint for ≈5.3 h, whereas no fixed flow in the 0.5–5.0 L min^{−1} range ever reaches it: feedback control is a structural enabler, not an optimisation. On overcast days, the threshold is never reached and charging heat collapses to zero, leaving a PV-only generator. Exergy delivery is nonetheless nearly controller-independent: the accumulated exergy delivery deficit after a 50% irradiance drop is 937 kJ, a controller-independent value changing only 1.3% across a systematic 4 × 4 gain sweep (K_p 0.33–2.7×, K_d 0.25–5× of nominal), and predictive control improves it by ≤1%. For PVT–TCES at this scale, the decisive lever is deployability, not control sophistication.

Academic Editor: Carlo Renno

Received: 11 June 2026

Revised: 11 July 2026

Accepted: 17 July 2026

Published: 21 July 2026

Copyright: © 2026 by the authors. Licensee MDPI, Basel, Switzerland. This article is an open access article distributed under the terms and conditions of the [Creative Commons Attribution \(CC BY\)](https://creativecommons.org/licenses/by/4.0/) license.

Keywords: photovoltaic–thermal collector; thermochemical energy storage; PID control; exergy delivery deficit; anti-windup; solar intermittency; $\text{SrCl}_2/\text{NH}_3$ working pair; performance benchmark; partial experimental validation

1. Introduction

The global push towards decarbonisation has positioned solar energy as a cornerstone of modern energy systems. Photovoltaics accounted for approximately 58% of new electricity-generating capacity additions worldwide in 2024 [1]. Despite this expansion, a conventional silicon PV module utilises only 15–20% of the incident solar energy as electricity; the remaining 65–70% of the absorbed spectrum is dissipated as waste heat that raises cell temperature and degrades electrical efficiency by approximately 0.2–0.5% per kelvin [2]. This dissipated fraction is an exergy loss that standalone PV cannot recover.

Photovoltaic–thermal (PVT) collectors address this loss directly by simultaneously recovering electrical and thermal energy from the same aperture. The HTF circulating behind the PV cells cools the cells (recovering electrical efficiency) while extracting useful thermal energy at 40–85 °C [3]. With a suitable downstream demand, integrated PVT systems can approach a solar fraction of unity [4]. However, the technology is thermodynamically attractive but commercially under-deployed: the IEA Solar Heating and Cooling Programme reports that the total installed PVT collector area worldwide reached only 1.6 million m² by the end of 2023, with newly installed PVT area falling by 30.3% in that single year [5].

This under-deployment is not attributable to electrical performance but to the limited usability of the recovered thermal stream. PVT output at 40–85 °C is too low-grade for most industrial processes and temporally misaligned with building heating demand, which peaks in winter when the solar resource is weakest. Without a compelling, dispatchable end-use, the economic case for PVT over standalone PV is weak [6].

Thermochemical energy storage (TCES) changes this picture fundamentally. TCES stores thermal energy as chemical potential through reversible endothermic–exothermic reactions, offering energy densities five to ten times higher than sensible or latent heat storage, negligible heat loss during storage, and theoretically unlimited storage duration [7]. Sorption-based TCES systems operate within the temperature range produced by PVT collectors, and on discharge deliver useful heat for space heating or drive absorption–cooling cycles [8]. Prior work by the present author demonstrated this potential through the integration of a compressor-assisted thermochemical sorption energy storage system with a PVT collector using $\text{SrCl}_2/\text{NH}_3$ [9,10], extending seasonal thermochemical-sorption storage concepts demonstrated for domestic dwellings [11].

A critical enabling condition for this integration has not been addressed in the literature. TCES reactions are governed by thermodynamic equilibrium: the HTF supplied from the PVT collector must keep the reactor above a chemistry-dependent threshold to sustain desorption. If the HTF temperature falls below this threshold—even transiently—the endothermic reaction stalls, conversion becomes incomplete, and in salt hydrate systems such as SrCl_2 , repeated incomplete cycles cause deliquescence and agglomeration that progressively degrade material performance [12]. A proportional–integral–derivative (PID) controller acting on HTF flow rate is the natural first approach. Çetin et al. [13] demonstrated PID-based exergy maximisation in a geothermal power plant; however, geothermal systems have thermally stable inputs, and their disturbance tests are manually assigned step changes, not the rapid, stochastic irradiance transients of solar operation. No existing study has evaluated PID flow rate control in a PVT–TCES integrated system

under dynamic solar intermittency, nor quantified the exergy delivery deficit that accumulates during the transient period between an irradiance disturbance and PID recovery.

This paper addresses that gap, supplying the lower anchor of a coordinated effort to bracket the achievable performance of this system class. A feedback-only PID controller cannot anticipate a disturbance; it can only react once the disturbance has propagated to the measured outlet temperature. Its performance therefore constitutes a physically meaningful lower bound on what any more sophisticated—necessarily anticipatory—controller can achieve. The present study characterises that lower bound rigorously on an experimentally grounded plant, so that the value added by predictive control is measured against a trustworthy reference rather than a loosely tuned strawman. The contributions are five-fold. First, we establish that closed-loop flow control is a structural enabling condition for PVT–TCES operation, not a peripheral optimisation: no fixed flow rate in the practical range brings the reactor within 47 K of its desorption threshold. Second, we define and quantify the transient exergy delivery deficit metric $\Delta Ex_{\text{deficit}}$ and show, through a systematic 4×4 gain sweep (K_p 0.33–2.7 $\times$ and K_d 0.25–5 $\times$ of nominal), that it possesses a controller-independent lower bound over the gain space—937 kJ on the jointly validated plant—that no PID re-tuning can breach, providing a reusable benchmark for the configuration. Third, all second-law quantities are computed with the rigorous Bejan/Kotas integrated thermal-exergy formulation, which we show corrects a systematic +22% overestimate embedded in the constant-temperature approximation widely used in PVT exergy work. Fourth, the analysis is conducted under measured-like stochastic tropical–coastal irradiance, corroborated against real monsoon days of differing clearness and variability, and uses conditional-integration anti-windup—features that prior geothermal PID-exergy studies do not exercise. Fifth, the collector model is partially validated against measured data from the same tropical–coastal site—a rooftop PV module-temperature record and a purpose-built, PID-controlled PVT prototype (Section 3.6)—so that the 937 kJ benchmark and the PID baseline rest on a plant grounded in measured physics rather than on a purely synthetic construct. A companion comparison on the same validated plant (Section 5.3) shows that anticipatory and model-free predictive controllers improve on this benchmark by at most about 1% under synthetic forcing and 0.07–0.24% on measured monsoon days; for PVT–TCES at this scale, the practical lever is therefore deployability, not control sophistication. Table 1 positions the present work against the six closest published studies, and the modelled system is shown in Figure 1 (the eight model nodes are numbered ①–⑧).

Table 1. Positioning of the present work against six closest published studies. $\checkmark$ = criterion addressed; $\times$ = criterion not addressed; “partly” denotes partial treatment; parenthetical notes qualify the entry.

Study	PVT?	TCES Coupling?	Closed-Loop PID?	Sub-Min Intermittency?	ΔEx of Recovery?
Tiwari & Sodha 2006 [14]	$\checkmark$	$\times$	$\times$	$\times$	$\times$
Keçebaş 2013 [15]/ Çetin 2021 [13]	$\times$ (geothermal)	$\times$	$\checkmark$	$\times$	$\times$
Badescu 2007 [16]	partly	$\times$	$\times$ (Pontryagin)	$\times$	$\times$
Araújo & Pereira 2017 [17]	$\times$ (FPC)	$\times$	$\checkmark$ (P-only)	$\times$	$\times$
Jiang et al. 2022 [18]	$\checkmark$	$\checkmark$	$\times$	$\times$	$\times$
Zou et al. 2025 [19]	$\checkmark$	$\times$ (HP)	$\times$	$\checkmark$	$\times$
Present work	$\checkmark$	$\checkmark$	$\checkmark$	$\checkmark$	$\checkmark$ (Equation (14))

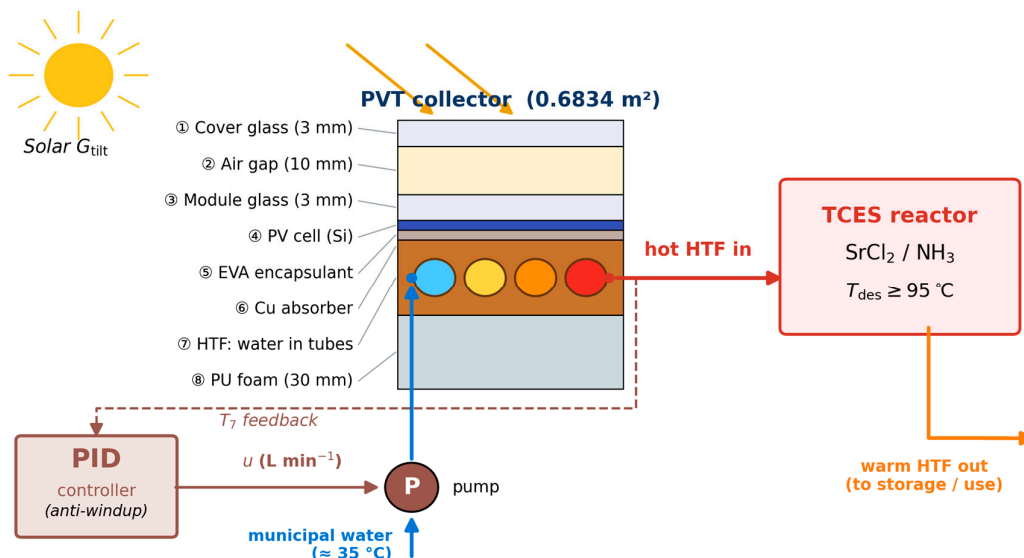


Figure 1. System schematic. The 0.6834 m² PVT collector is modelled as an eight-layer lumped-capacitance stack (nodes ①–⑧; Equations (6) and (7) and Appendix B) with water flowing through circular absorber tubes. The PID controller acts on the measured HTF outlet temperature T_7 and modulates the pump flow rate u . The system is operated once-through (open loop): municipal water enters the collector at ≈ 35 °C and is delivered as hot HTF to the $\text{SrCl}_2/\text{NH}_3$ TCES reactor at the ≈ 95 °C desorption setpoint, while the warm HTF leaving the reactor is sent to storage or use rather than recirculated; closed-loop recirculation is left to future work.

2. Background and Related Work

2.1. PVT Exergy and the Flow Rate Optimum

The relationship between HTF flow rate and PVT exergy output has been studied extensively under steady-state conditions [14,20–22]. These results consistently show that the exergy-optimal flow rate shifts with irradiance—which motivates closed-loop adjustment in real operation. A sensitivity analysis of a hybrid thermochemical-sorption TES system integrated with PVT [18] showed that a 20 K difference in PVT outlet temperature produced a measurable shift in overall exergy efficiency; under intermittent solar irradiance, this 20 K swing occurs repeatedly in fixed-flow-rate systems.

2.2. PID Control for Exergy Maximisation via Flow Rate

PID-based flow rate control for exergy maximisation originates in geothermal district-heating research [15,23]. Çetin et al. [13] extended this to a geothermal power plant, with reported gains of 25% in exergy efficiency and 23% in power output. All three, however, address thermally stable geothermal sources with manually assigned step disturbances, not solar transients; gains tuned for such slow dynamics would be fundamentally mismatched to a solar-driven PVT system. In the solar-thermal domain, Badescu [16] proposed an optimal flow rate control strategy whose Pontryagin-maximum-principle formulation is not well-suited to real-time implementation; Araújo and Pereira [17] showed that proportional flow rate control improves solar domestic hot water performance but did not address TCES coupling.

2.3. Intermittency as a Control Challenge

Cloud transients generate irradiance ramp rates at the array level of 10–100 kW s^{−1} [24]. Most PVT studies treat solar irradiance as a quasi-static parameter [22]. Yan et al. [25]

found that feedwater flow rate in a solar-aided coal-fired power plant must track solar reductions within <200 s; beyond this lag, measurable exergy destruction accumulates. Zou et al. [19] showed that a 2 m³ thermal-storage tank suppresses PVT-outlet fluctuations by 46.7%—but only for transients slower than the storage thermal time constant. For faster transients, active flow rate control is the only mechanism available.

2.4. TCES Temperature Requirements

TCES desorption requires the reactor temperature to exceed an equilibrium threshold determined by the condenser pressure [7,8]. From a second-law perspective, the exergy delivered to the reactor is proportional to the temperature differential above the ambient dead state; a reduction in HTF outlet temperature during a transient simultaneously reduces both the rate of exergy delivery and the reaction driving force. These are compounding, irrecoverable losses, which makes the PID response time to irradiance disturbances the single most important control performance metric for this application.

2.5. Research Gaps Motivating This Study

Three non-overlapping gaps emerge. (i) PID-based flow rate control for exergy maximisation has been validated only for geothermal systems with stable heat inputs, never under the rapid stochastic forcing of solar operation. (ii) PVT exergy analyses have established the irradiance dependence of the optimal flow rate but have not investigated the dynamic response under realistic intermittent irradiance, nor have they adopted the rigorous integrated exergy accounting that high per-pass temperature rises demand. (iii) PVT–TCES integration studies have demonstrated thermodynamic feasibility but have not addressed the closed-loop flow rate control problem required to protect TCES reaction conditions. No existing study quantifies the exergy delivery deficit accumulated during PID recovery transients, identifies the critical disturbance conditions under which PID protection fails, or establishes a reusable feedback-only benchmark against which anticipatory controllers can be judged. The present work closes all three gaps for the SrCl₂/NH₃ configuration and, by characterising the feedback-only benchmark, supplies the reference point that gives the subsequent predictive-control results their meaning.

3. Methodology

The integrated simulation framework comprises five components: (i) a solar geometry and irradiance transposition model; (ii) an eight-layer PVT physics model; (iii) a PID controller operating on outlet fluid temperature via flow rate manipulation; (iv) an energy and exergy performance analyser; and (v) five case-study scenarios. Implementation is in Python 3.11.7 (NumPy 1.26.4, SciPy 1.11.4, Matplotlib 3.8.0); a reproducibility script is released alongside this paper.

3.1. Solar Irradiance Model

The simulation runs from 06:00 to 18:00 local solar time. Clear-sky global horizontal irradiance (GHI) is modelled as a sinusoidal approximation of the diurnal solar arc:

$$G_{\text{clear}}(t) = 1000 \sin \left[\frac{\pi(t-6)}{12} \right] \quad (1)$$

Solar intermittency is superimposed by adding zero-mean Gaussian noise of standard deviation $\sigma_G = 120 \text{ W m}^{-2}$ (representative of cumulus cloud shadows at the study site), with the result clipped to a physically meaningful range:

$$G_h(t) = \text{clip} [G_{\text{clear}}(t) + \mathcal{N}(0, \sigma_G^2), 0, 1100] \quad (2)$$

In Equation (2), $\mathcal{N}(0, \sigma_G^2)$ denotes an independent zero-mean Gaussian (normal) random draw of variance σ_G^2 applied at each control step, G_{clear} is the deterministic clear-

sky envelope of Equation (1), G_h is the resulting global horizontal irradiance (GHI), and the clip[·] operator bounds the result to the physically admissible range 0–1100 W m⁻².

Figure 2 plots a single realisation of the stochastic forcing alongside the corresponding plane-of-array (POA) irradiance; its realism is corroborated in Section 3.6, where measured POA irradiance for two monsoon days of differing variability at the same site shares the diurnal envelope and cloud-driven intermittency (Figure 3).

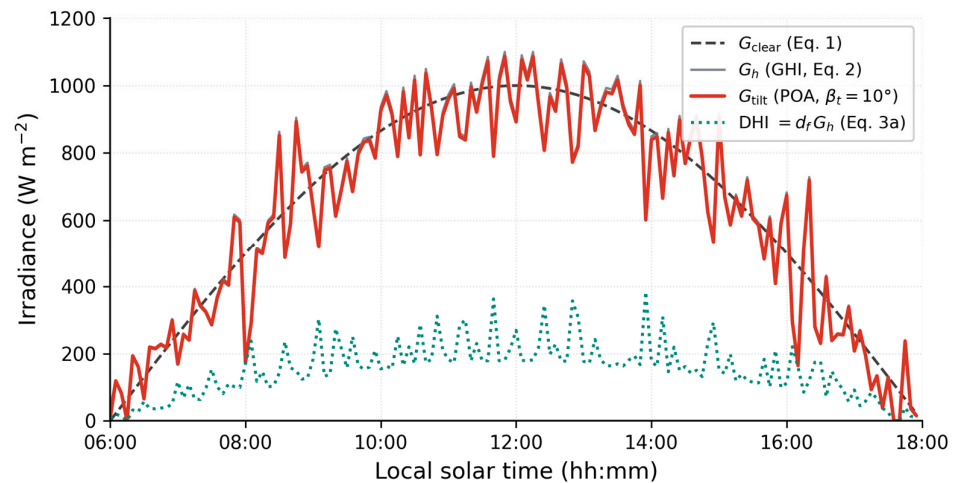


Figure 2. Stochastic global horizontal irradiance G_h (GHI) and plane-of-array irradiance G_{tilt} (POA) used as forcing input to the PID controller (seed = 1, synthetic day $n_d = 81$; time axes in hh:mm local solar time here and in all clock-time figures), together with the deterministic clear-sky envelope G_{clear} of Equation (1) and the diffuse component $\text{DHI} = d_f G_h$ of Equation (3a). The POA tracks the GHI closely owing to the small collector tilt (10°) and the low site latitude (8.64° N).

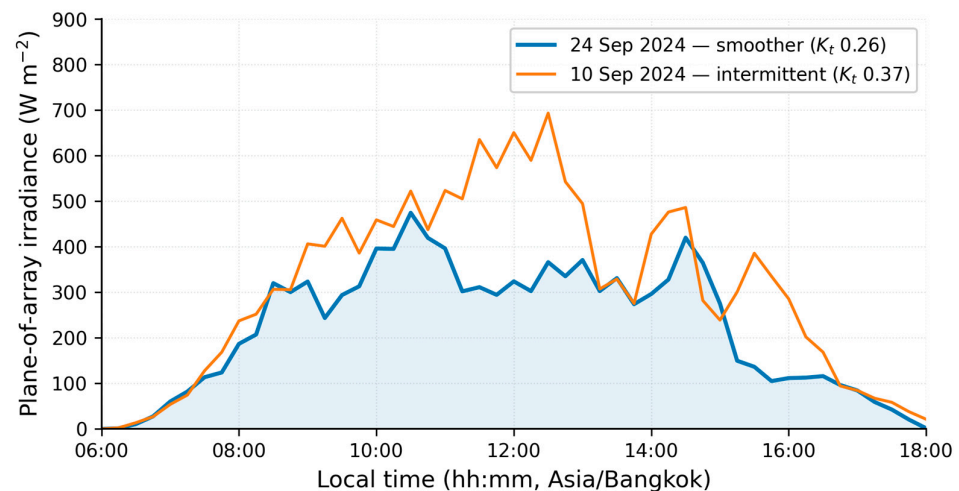


Figure 3. Measured plane-of-array irradiance at the Walailak University site for the two September 2024 prototype-campaign days—a smoother day (24 September, clearness index 0.26) and a more intermittent day (10 September, 0.37)—reconstructed from the rooftop pyranometers by the same Erbs/isotropic transposition as Figure 2. Both fall in a cloudy monsoon fortnight (neither is clear-sky); isolated 15 min dropouts are removed and the series lightly smoothed for display.

The simulation date is $n_d = 81$ (vernal equinox, 22 March), representative of the annual-mean solar geometry for the site. The POA irradiance is obtained by isotropic-sky

transposition [26], with the diffuse fraction d_f computed from the full three-branch Erbs (1982) correlation [27]:

$$d_f = \begin{cases} 1 - 0.09 k_t, & k_t \leq 0.22 \\ 0.9511 - 0.1604 k_t + 4.388 k_t^2 - 16.638 k_t^3 + 12.336 k_t^4, & 0.22 < k_t \leq 0.80 \\ 0.165, & k_t > 0.80 \end{cases} \quad (3)$$

The Python implementation of Equation (3) is given in Listing S1 (Supplementary Materials). The corresponding routine in the original notebook used only the first and third branches, biasing the POA irradiance upward by 30–80% for partly cloudy conditions ($0.22 < k_t < 0.80$); the corrected form is used throughout this study.

The diffuse fraction d_f splits the GHI into its diffuse and beam horizontal components, which are transposed to the plane of array by the isotropic-sky (Liu–Jordan) model [26]. With the day number n_d , solar declination $\delta = 23.45^\circ \sin [360^\circ(284 + n_d)/365]$, hour angle $\omega = 15^\circ(t - 12)$ and zenith angle given by $\cos \theta_z = \sin \varphi \sin \delta + \cos \varphi \cos \delta \cos \omega$ at the site latitude $\varphi = 8.643^\circ$ N, the instantaneous clearness index is $k_t = G_h/(G_{sc} \cos \theta_z)$, with $G_{sc} = 1367 \text{ W m}^{-2}$; then

$$\text{DHI} = d_f G_h, \quad \text{BHI} = G_h - \text{DHI} \quad (3a)$$

$$G_{\text{tilt}} = \text{BHI} R_b + \text{DHI} \frac{1 + \cos \beta_t}{2} + G_h \rho \frac{1 - \cos \beta_t}{2}, \quad R_b = \frac{\cos \theta}{\cos \theta_z} \quad (3b)$$

where $\beta_t = 10^\circ$ is the collector tilt, $\rho = 0.20$ the ground albedo and $\cos \theta = \cos \theta_z \cos \beta_t$ for the south-facing collector. Equation (3b) defines the plane-of-array irradiance G_{tilt} used throughout (Equations (5), (6), (13a) and (14)); at this small tilt and low latitude $R_b \approx 1$, which is why the POA tracks the GHI closely in Figures 2 and 3.

3.2. Eight-Layer PVT Model

The collector is modelled as a one-dimensional lumped-capacitance system comprising eight thermal nodes, numbered ①–⑧ in Figure 1: (1) cover glass, (2) air gap, (3) module glass, (4) PV cell, (5) EVA adhesive, (6) copper absorber and tube, (7) HTF, and (8) PU foam back-insulation. The PV electrical efficiency is temperature-dependent:

$$\eta(T_4) = \eta_{\text{ref}} [1 - \beta (T_4 - T_{\text{ref}})] \quad (4)$$

$$P_{\text{elec}} = \eta(T_4) G_{\text{tilt}} \tau_{\text{cov}} \tau_{\text{mod}} A \quad (5)$$

The energy balance equation for the PV-cell node (illustrative of the formulation; the complete eight-equation system is written out in Appendix B and implemented in Listing S2 of the Supplementary Materials) is:

$$m_4 c_4 \frac{dT_4}{dt} = \alpha_{\text{pv}} \tau_{\text{cov}} \tau_{\text{mod}} G_{\text{tilt}} A - P_{\text{elec}} + h_{\text{cond}} A (T_3 - T_4) - h_{\text{cond}} A (T_4 - T_5) \quad (6)$$

Here $h_{\text{cond}} = 200 \text{ W m}^{-2} \text{ K}^{-1}$ is the solid inter-layer conduction coefficient (Table 2), applied over the aperture area A to each internal solid–solid link (nodes ③–④, ④–⑤, ⑤–⑥ and ⑥–⑧); the glazing stack (nodes ①–②–③) is coupled through the air-gap coefficient $h_{\text{gap}} = 5.5 \text{ W m}^{-2} \text{ K}^{-1}$, and the cover glass absorbs a fraction $\alpha_{\text{glass}} = 0.04$ of the incident irradiance. The complete eight-equation system is written out in Appendix B, and the nodal masses and heat capacities are listed in Table 3.

The fluid-node energy balance (which is the algebraic linkage to the PID actuator) is:

$$m_7 c_7 \frac{dT_7}{dt} = h_{\text{fluid}} A_{\text{tube}} (T_6 - T_7) - \dot{m} c_{p,w} (T_7 - T_{\text{in}}) \quad (7)$$

The useful thermal heat removal rate that defines the input to the TCES reactor is:

$$\dot{Q}_{\text{th}}(t) = \dot{m}(t) c_{p,w} [T_7(t) - T_{\text{in}}] \quad (8)$$

The front-side convection coefficient depends on wind speed via the McAdams correlation, $h_{\text{conv}}(v) = 5.7 + 3.8 v$ (in $\text{W m}^{-2} \text{K}^{-1}$), so that the simulation responds correctly to the diurnal wind profile:

$$h_{\text{conv}}(v) = 5.7 + 3.8 v \quad (9)$$

The eight coupled ODEs are integrated over each control interval $\Delta t = 10$ s using `scipy.integrate.odeint` with relative and absolute tolerances of 1×10^{-6} and 1×10^{-8} respectively and a maximum internal step size of 1 s. Initial conditions are set to the ambient temperature at simulation start (06:00). Physical and control parameters are listed in Table 2.

Table 2. System parameters (PVT collector and PID controller).

Parameter	Symbol	Value	Unit
Collector aperture area	A	0.6834	m^2
Tube–fluid contact area	A_{tube}	0.41	m^2
Reference electrical efficiency (STC)	η_{ref}	0.16	–
Temperature coefficient	β	0.0045	K^{-1}
STC reference temperature	T_{ref}	25	$^{\circ}\text{C}$
Sky radiation coefficient	h_{rad}	5.0	$\text{W m}^{-2} \text{K}^{-1}$
Tube-to-fluid convection	h_{fluid}	350	$\text{W m}^{-2} \text{K}^{-1}$
PU foam back loss coefficient	h_{ins}	0.83	$\text{W m}^{-2} \text{K}^{-1}$
Inter-layer (solid) conduction coefficient	h_{cond}	200	$\text{W m}^{-2} \text{K}^{-1}$
Air-gap conduction coefficient	h_{gap}	5.5	$\text{W m}^{-2} \text{K}^{-1}$
Cover-glass absorptance	α_{glass}	0.04	–
Cover glass transmittance	τ_{cov}	0.91	–
Module glass transmittance	τ_{mod}	0.92	–
PV cell absorptance	α_{pv}	0.90	–
HTF inlet temperature (municipal once-through feed)	T_{in}	35	$^{\circ}\text{C}$
Exergy dead-state (reference) temperature	$T_{\text{a}}(t)$	instantaneous ambient	$^{\circ}\text{C}$
PID setpoint (TCES threshold)	T_{sp}	95	$^{\circ}\text{C}$
PID proportional gain	K_{p}	0.015	–
PID integral gain	K_{i}	2×10^{-4}	–
PID derivative gain	K_{d}	0.20	–
Control timestep	Δt	10	s
Pump flow range	$[u_{\text{min}}, u_{\text{max}}]$	$[0.05, 0.5]$	L min^{-1}

The modelled aperture $A = 0.6834 \text{ m}^2$ is the reference collector used consistently throughout this modelling programme; the experimental prototype of Section 3.6 is a larger unit (full Jinko JKM355PP-72 module, $\approx 1.94 \text{ m}^2$ aperture) used to validate the intensive collector heat transfer physics at a different scale. Because every reported efficiency is a per-unit-area ratio, a $2.8\times$ change in aperture ($0.6834 \rightarrow 1.94 \text{ m}^2$, with nodal masses and flow limits scaled) changes the first- and second-law efficiencies by less than 0.5%, while absolute yields scale linearly.

Table 3. Nodal masses, specific heats and lumped heat capacities of the eight-node model (nodes ①–⑧ of Figure 1; transcribed from the released implementation).

Node	Component	m (kg)	c (J kg ⁻¹ K ⁻¹)	m·c (J K ⁻¹)
①	Cover glass	5.1	800	4080
②	Air gap	0.008	1005	8.0
③	Module glass	5.1	800	4080
④	PV cell	0.5	800	400
⑤	EVA adhesive	0.5	2000	1000
⑥	Copper absorber + tube	3.0	385	1155
⑦	HTF (water, 0.8 L)	0.8	4180	3344
⑧	PU foam back	0.82	1500	1230

3.3. PID Controller

A discrete-time PID controller regulates the pump flow rate $u(k)$ with the objective of holding the HTF outlet temperature T_7 at the setpoint $T_{sp} = 95\text{ }^{\circ}\text{C}$, corresponding to the $\text{SrCl}_2/\text{NH}_3$ desorption threshold. The error and the control law are:

$$e(k) = T_7(k) - T_{sp} \quad (10)$$

$$u(k) = K_p e(k) + K_i \sum_{j=0}^k e(j) \Delta t + K_d \frac{e(k) - e(k-1)}{\Delta t} \quad (11)$$

In Equation (11), k is the current discrete control step, j is the summation (integration) index running over control steps $0, \dots, k$, $e(j)$ is the outlet temperature error of Equation (10) at step j , and $\Delta t = 10\text{ s}$ is the control interval, so the middle term is the accumulated integral error. The regulated (controlled) variable is the outlet temperature T_7 —a measurable proxy for the reactor desorption threshold constraint—not exergy directly; the energy and exergy quantities of Section 3.4 are performance evaluation metrics computed from the resulting trajectories, not control objectives.

The controller output is bounded to $[u_{\min}, u_{\max}] = [0.05, 0.5]\text{ L min}^{-1}$. Conditional-integration anti-windup is implemented: when saturated at $u_{\max}$, the integral updates only if $e < 0$; when saturated at $u_{\min}$, only if $e > 0$. This prevents exergy-destroying overshoot when the controller emerges from a saturated state—a condition that recurs repeatedly under cloud transients. The PID implementation is given in Listing S3 (Supplementary Materials).

The control variable u is treated as an idealised bounded flow command; the physical actuator is a small variable-speed circulation pump, and its finite turndown and minimum stable flow are the practical constraints on realising the low-flow regime the controller commands. The $0.05\text{--}0.10\text{ L min}^{-1}$ active band identified below is not a modelling abstraction: the co-located PVT prototype of Section 3.6 is driven by a 12 V DC circulation pump that sustains stable outlet temperature regulation in precisely this band under monsoon operation, demonstrating that an embedded-systems-cost pump can deliver the ultra-low flow on which the desorption threshold temperature rise depends. The $\approx 60\text{ K}$ per-pass rise required to reach the TCES setpoint is achievable only at flows roughly an order of magnitude below the typical PVT design point; that this is the regime in which small pumps approach their low-flow stability limit is precisely why deployability, not control sophistication, is the decisive lever for this system class (Section 5.1).

The gain set was selected by manual sensitivity tuning informed by the plant's identified input-to-outlet response time: at the noon operating point, a step in irradiance or flow relaxes with an effective 63% response time $\tau_{\text{eff}} \approx 13\text{--}15\text{ min}$, dominated by the thermally coupled glazing stack (the bare absorber-plus-fluid estimate $(m_{6C6} + m_{7C7})/(h_{\text{fluid}} A_{\text{tube}}) \approx 31\text{ s}$ is far shorter; Table 3, Appendix B). Gains an order of magnitude faster or slower than the adopted set were rejected as noise-amplifying or sluggish. We do not

claim a formal optimal-tuning procedure: the 4×4 sweep of Section 4.5 confirms a posteriori that the principal transient metrics vary by at most a few percent across the swept range, so the central conclusions do not depend on the precise gain values.

3.4. Energy and Exergy Performance Metrics

Throughout the exergy analysis the dead state (reference environment) is the instantaneous ambient temperature $T_a(t)$ —the same time-varying ambient that forces the collector energy balance (Table 2); it is distinct from the STC reference temperature $T_{\text{ref}} = 25^\circ\text{C}$ of the PV model (Equation (4)) and from the HTF inlet temperature $T_{\text{in}} = 35^\circ\text{C}$. All exergy efficiencies reported here are gross values: they are referenced to the collector control volume (aperture to HTF outlet) and exclude pump electrical consumption. At the throttled operating band ($0.05\text{--}0.10\text{ L min}^{-1}$) the pumping penalty is small—the ideal hydraulic exergy is $\approx 0.03\text{ W}$ ($\approx 0.007\%$ of the daily exergy yield), and even a realistic 12 V DC circulation pump drawing $\approx 5\text{ W}$ while energised consumes $\approx 7\text{ Wh day}^{-1}$ at the 11.6% mean duty cycle, i.e., $\approx 1\%$ of the total daily exergy yield—so the net exergy efficiency lies within about one relative percent of the gross value quoted throughout.

The thermal exergy delivered to the TCES reactor is computed from the rigorous Bejan/Kotas integrated form [28], which accounts for the cooling of the HTF as it transfers heat from T_7 to T_{in} :

$$\dot{E}x_{\text{th}}(t) = \dot{m}(t) c_{p,w} \left\{ [T_7(t) - T_{\text{in}}] - T_a(t) \ln \frac{T_7(t)}{T_{\text{in}}} \right\} \quad (12)$$

The implementation is given in Listing S4 (Supplementary Materials). The constant-temperature approximation used in earlier PVT exergy work overestimates the thermal exergy by about 22% at the present operating point (Section 4.1, Figure S6).

The solar exergy uses the temperature-dependent Petela coefficient [29]:

$$\psi(T_a) = 1 - \frac{4}{3} \left(\frac{T_a}{T_{\odot}} \right) + \frac{1}{3} \left(\frac{T_a}{T_{\odot}} \right)^4 \quad (13)$$

The solar and electrical exergy rates follow directly—electricity is pure exergy, and the solar exergy is the Petela-weighted plane-of-array irradiance:

$$\dot{E}x_{\text{solar}}(t) = \psi(T_a(t)) G_{\text{tilt}}(t) A \quad (13a)$$

$$\dot{E}x_{\text{elec}}(t) = P_{\text{elec}}(t) \quad (13b)$$

Writing a second-law (exergy) balance over the collector control volume—bounded by the aperture (solar exergy inflow) and the HTF outlet (thermal exergy outflow to the reactor), with dead state $T_a(t)$ —gives $\dot{E}x_{\text{solar}} = \dot{E}x_{\text{elec}} + \dot{E}x_{\text{th}} + \dot{E}x_{\text{loss}} + T_a \dot{S}_{\text{gen}}$, where $\dot{E}x_{\text{loss}}$ collects the exergy carried away by the convective, radiative and back-insulation losses and $T_a \dot{S}_{\text{gen}}$ is the internal irreversibility. The principal novelty metric of this paper is the exergy that fails to reach the reactor, relative to the undisturbed baseline, during a PID recovery transient—the accumulated exergy delivery deficit:

$$\Delta Ex_{\text{deficit}} = \int_{t_{\text{dist}}}^{t_{\text{settle}}} \left[\psi(T_a(t)) G_{\text{tilt}}(t) A - P_{\text{elec}}(t) - \dot{m}(t) c_{p,w} \left\{ (T_7(t) - T_{\text{in}}) - T_a(t) \ln \frac{T_7(t)}{T_{\text{in}}} \right\} \right] dt \quad (14)$$

with t_{dist} the disturbance onset, t_{settle} the time at which T_7 returns to within 1 K of T_{sp} , and the three integrand terms given by Equations (13a), (13b) and (12) at the common dead state $T_a(t)$. By the control-volume balance above, the integrand equals $\dot{E}x_{\text{loss}} + T_a \dot{S}_{\text{gen}}$: the metric measures undelivered exergy relative to the undisturbed baseline, not internal irreversibility alone—during an irradiance reduction it is dominated by the un-captured solar term, the true irreversibility being only the recovery-transient subset. This is precisely why, as shown in Section 4.5, $\Delta Ex_{\text{deficit}}$ is a controller-independent benchmark for the configuration: the controller can act only on the small recovery-transient term.

3.5. Case Studies

Table 4 summarises the five case-study scenarios examined in this section; each case isolates one aspect of the controller's dynamic response and its exergy consequences.

Table 4. Summary of the five case-study scenarios.

Case	Disturbance/Setup	Reported Metrics
1	Full-day stochastic forcing, Monte-Carlo ($N = 30$ seeds)	Daily energy + exergy yields, peak temperatures, time at T_{sp}
2	50% step reduction in G_{tilt} at solar noon, 30 min hold	Settling time, undershoot, overshoot, $\Delta Ex_{\text{deficit}}$ (Equation (14))
3	Sustained linear ramps at $-1, -2, -3, -5 \text{ W m}^{-2} \text{ s}^{-1}$ for 5 min	Minimum HTF outlet temperature, threshold-breach assessment
4	PID vs. fixed flow at $u \in \{0.5, 1.0, 2.0, 5.0\} \text{ L min}^{-1}$	Peak T_{out} , peak T_{pv} , time at T_{sp}
5	4×4 grid in (K_p, K_d) , $0.33\text{--}2.7\times$ and $0.25\text{--}5\times$ of nominal	Heat-maps of settling time, $\Delta Ex_{\text{deficit}}$, overshoot

3.6. Experimental Validation and Real-Weather Forcing

The thermal model of Section 3.2 and the irradiance forcing of Section 3.1 are grounded here against measured data from the study site. Two independent sources are used. The first is a three-year record (March 2023 to June 2026) from the Walailak University rooftop monitoring system of a 2.54 MWp grid-connected photovoltaic plant, logging plane-of-array irradiance from two independent pyranometers (and their geometric mean), ambient temperature, module temperature and wind speed at 15 min resolution. The second is a purpose-built single-panel PVT collector—a Jinko JKM355PP-72 module fitted with a copper-tube absorber, foam-insulated back and glazed front, regulated by a PID-controlled 12 V DC circulation pump acting on the outlet water temperature and operating in the same $0.05\text{--}0.10 \text{ L min}^{-1}$ band as the simulated controller command (Section 3.3)—constructed and instrumented at the same site. The prototype module ($\approx 1.94 \text{ m}^2$ aperture) is larger than the 0.6834 m^2 modelled reference collector; because the validation compares temperatures and per-area heat transfer coefficients, it tests the collector heat transfer physics at a different scale rather than the modelled once-through temperature rise. Because the prototype construction (cover $\rightarrow$ air gap $\rightarrow$ module glass $\rightarrow$ cell $\rightarrow$ absorber/tube $\rightarrow$ fluid $\rightarrow$ insulated back) mirrors the eight-node stack of Section 3.2, and because its pump controller is a physical instance of the flow rate feedback law studied in this paper, it provides a partial but well-posed hardware test of both the collector physics and the control concept.

Measured forcing is reconstructed by transposing the WU global horizontal irradiance to the plane of array with the same optics used in Section 3.1 (Erbs diffuse split, isotropic sky). Figure 3 overlays the two September 2024 prototype-campaign days, which differ in daily clearness and sub-daily variability (both cloud-affected—neither is a clear-sky day). The measured envelope and its cloud-driven excursions are consistent with the synthetic forcing of Figure 2, confirming that the $\sigma = 120 \text{ W m}^{-2}$ stochastic model is representative of the site; the three-year record additionally supplies many genuinely clear and intermittent days that are retained as drop-in forcing for the planned multi-day analysis. Note that the smoother campaign day (24 September, $K_t = 0.26$) has a lower daily clearness index than the more intermittent day (10 September, $K_t = 0.37$): within a cloudy monsoon fortnight, daily transmittance and sub-daily variability are only weakly correlated, so the day with fewer fast fluctuations is not necessarily the brighter one. Table 5 collates every measured and synthetic day used in this study.

Table 5. All irradiance days used in this study.

Label	Date	Source	K_t	VI	Role
Synthetic stochastic day	$n_d = 81$ (22 March)	Equations (1) and (2), $\sigma_G = 120 \text{ W m}^{-2}$	≈ 0.7	–	Cases 1–5 forcing (Section 4)
Synthetic clear archetype	–	shared-plant generator	0.74	1.03	Appendix A.2
Synthetic intermittent archetype	–	shared-plant generator	0.72	1.27	Appendix A.2
Measured—smoother	24 September 2024	WU pyranometers	0.26	low	Section 3.6 validation
Measured—intermittent	10 September 2024	WU pyranometers	0.37	high	Section 3.6 validation
Measured—clear-sky	23 February 2026	WU pyranometers	0.52	1.00	Section 6 output (abstract headline)
Measured—overcast	7 May 2024	WU pyranometers	0.13	1.00	Section 6 output

The front-side thermal core of the model—the optics and the convective/radiative balance of nodes 1–4, which set the cell operating temperature and hence both the $\eta(T)$ penalty and the exergy delivery deficit benchmark—is validated against the measured temperature of a bare rooftop PV module driven by the WU plane-of-array irradiance, ambient temperature and wind. Integrating the lumped energy balance $C \cdot dT_m/dt = (\alpha\tau - \eta(T_m)) \cdot G_{POA} - U(v) \cdot (T_m - T_a)$, with a single site-calibrated loss coefficient ($U \approx 12 \text{ W m}^{-2} \text{ K}^{-1}$, consistent with the restricted ventilation of the close-mounted array, NOCT-equivalent $\approx 57^\circ\text{C}$), reproduces the measured module temperature with an RMSE of 3.8°C , a mean bias of $+0.4^\circ\text{C}$ and $R^2 = 0.94$ over $n = 170$ fifteen-minute samples (Figure 4). The model captures both the diurnal envelope and the cloud-driven dips and relaxes to $1\text{--}2^\circ\text{C}$ below ambient at night, the correct radiative behaviour. This supports the optical and convective/radiative physics that underpins the exergy delivery deficit benchmark.

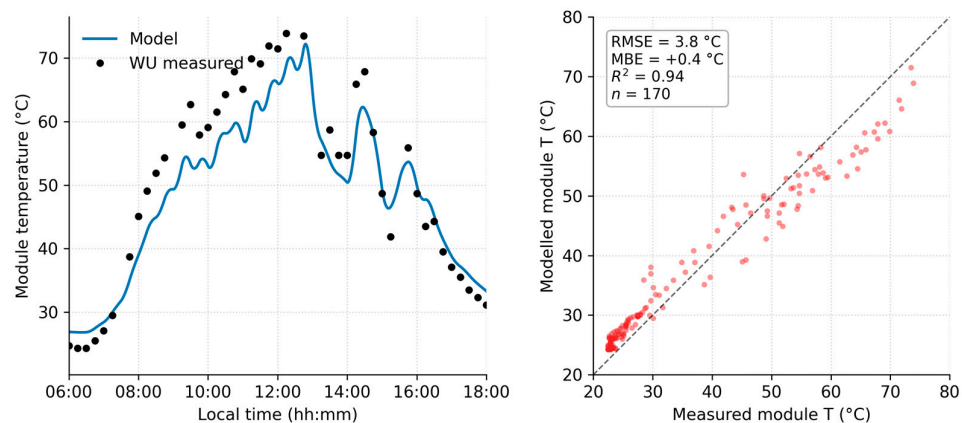


Figure 4. Validation of the collector thermal core against the measured temperature of a bare rooftop PV module at the study site. Left: modelled and measured module temperature over the intermittent day (10 September); right: parity plot over the two campaign days, 10 and 24 September (RMSE 3.8°C , MBE $+0.4^\circ\text{C}$, $R^2 = 0.94$, $n = 170$). A single site-calibrated loss coefficient ($U \approx 12 \text{ W m}^{-2} \text{ K}^{-1}$) is used.

Figure 5 shows the prototype rig: the glazed copper-tube PVT collector, the variable-speed circulation pump and its PID driver, and the control and data-acquisition panel. The controller modulates the 12 V pump to regulate the outlet water temperature—the same flow rate feedback principle analysed throughout this paper—and the rig was operated outdoors under September monsoon skies. On-site irradiance was not logged during these (rainy) campaigns, which is precisely why the co-located WU pyranometer record supplies the forcing for the prototype analysis.

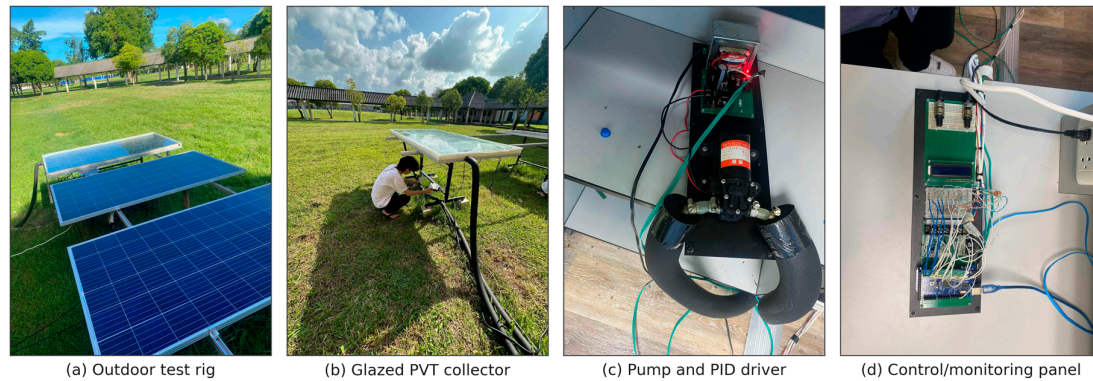


Figure 5. The Walailak University PVT prototype, a physical instance of the PID flow rate controller studied here: (a) the outdoor test rig; (b) the glazed copper-tube collector; (c) the 12 V circulation pump and its PID driver; (d) the control and data-acquisition panel. The collector construction mirrors the eight-node model of Section 3.2.

Because the prototype water inlet temperature was not logged, the panel temperature is predicted from the WU forcing using the measured outlet water temperature as the fluid boundary condition, which isolates the absorber-to-water heat transfer physics. On the well-cooled campaign day (24 September 2024, low-variability), the model reproduces the measured panel temperature with an RMSE of 1.3 °C ($n = 52$); the fitted absorber-to-water conductance, $UA_f \approx 85 \text{ W m}^{-2} \text{ K}^{-1}$, is high and consistent with well-bonded copper tubing (Figure 6, left). An adversarial baseline that simply equates panel and water temperature achieves only RMSE 2.1 °C, so the energy balance model improves the prediction by about 39% in RMSE—real structure rather than overfitting. On the intermittent day (10 September), with the pump cycling under fast cloud transients, the measured panel temperature sits well above the water temperature, and the fitted conductance collapses to $\approx 9 \text{ W m}^{-2} \text{ K}^{-1}$ (Figure 6, right): the model thus correctly reproduces flow-limited panel–water decoupling, the very mechanism that the flow rate controller of this paper exists to manage.

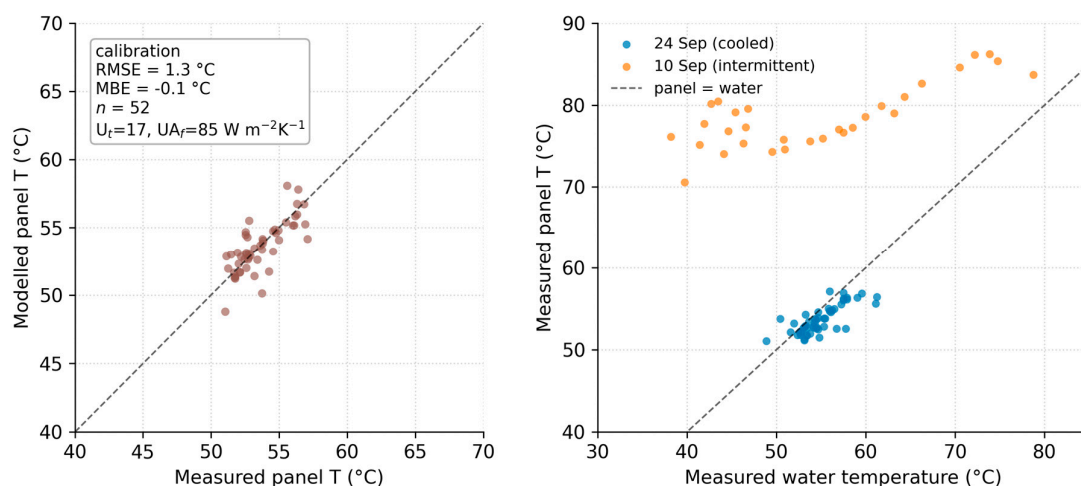


Figure 6. Validation of the prototype absorber-to-water heat transfer. (Left): modelled versus measured panel temperature on the well-cooled day, 24 September (RMSE 1.3 °C, $n = 52$; fitted $UA_f \approx 85 \text{ W m}^{-2} \text{ K}^{-1}$). (Right): measured panel temperature against measured water temperature on the cooled (24 September) and intermittent (10 September) days; the dashed line marks perfect panel–water coupling. On the intermittent day the panel decouples from the water (UA_f collapses to $\approx 9 \text{ W m}^{-2} \text{ K}^{-1}$), the flow- and transient-limited regime the controller is designed to manage. In the left

panel, the brown markers show the modelled-versus-measured panel-temperature pairs and the dashed line is the 1:1 diagonal.

Under the same monsoon conditions, the prototype delivered usable hot water—a peak outlet up to 79 °C—daily maxima of 46–79 °C across the four campaign days (61–79 °C on the three non-stagnant days) and daily means of 39–56 °C—while the PID modulated the pump flow (Figure 7). This is a real-world demonstration of the PVT-to-TCES charging service that the simulated system is designed to provide: the controller trades a measure of electrical efficiency for a thermal stream hot enough to approach the desorption threshold, exactly as in the simulation.

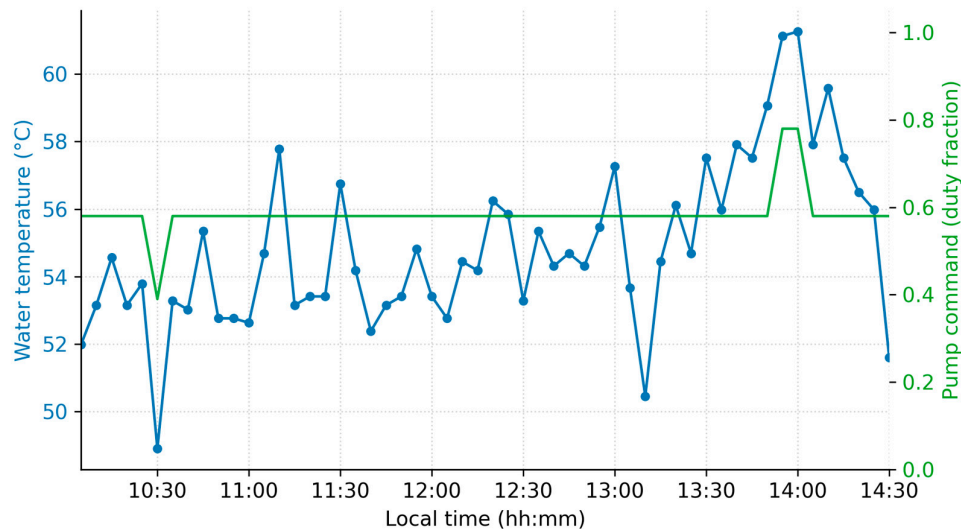


Figure 7. Demonstration of PID-regulated hot-water delivery by the prototype on a representative campaign day (24 September 2024): measured outlet water temperature (left axis) and commanded pump duty fraction (right axis). The collector sustained water temperatures in the 49–61 °C band on this day, with peaks to 79 °C across the campaign (10 September 2024), under monsoon irradiance.

The validation establishes the model’s thermal behaviour, not its electrical model: the prototype panel was not held at its maximum-power point, so the measured apparent efficiency (0.10–0.12, rising anomalously with temperature) could not validate the temperature-dependent $\eta(T)$ relation, which is supported by the manufacturer datasheet and enters the thermal balance weakly ($\eta \cdot G_{\text{tilt}} \ll \alpha \tau \cdot G_{\text{tilt}}$). The module validation addresses the front optical/convective core common to PV and PVT, and the cooled-collector validation demonstrates absorber/panel heat transfer rather than the full once-through rise; two of the four prototype days are flow-limited, and one module loss coefficient is site-calibrated. Within this scope the agreement is strong, and the central physics underpinning the 937 kJ deficit benchmark is experimentally supported.

4. Results

4.1. Case 1—Baseline Tracking Under Stochastic Irradiance

Figure S1 shows the closed-loop temperature response under a single realisation (seed = 1) of the stochastic irradiance forcing. After the morning warm-up of approximately 3.5 h, the controller reaches the 95 °C setpoint and holds the HTF outlet within +1.4 K for the remainder of the high-irradiance window. The PV cell temperature reaches 103.6 °C around solar noon—the deliberate trade-off of operating the panel hot to deliver heat at the $\text{SrCl}_2/\text{NH}_3$ desorption threshold.

Figure S2 shows the corresponding commanded pump flow rate. The controller operates in a narrow active band around $0.05\text{--}0.10\text{ L min}^{-1}$, with the $u_{\min} = 0.05\text{ L min}^{-1}$ bound active only during warm-up and at end-of-day. The very low actuator duty cycle is a direct consequence of the high temperature-rise constraint required by the TCES setpoint.

Figure S3 overlays the POA irradiance and the PV electrical power, which peaks at about 60 W around solar noon; the 5–10 W oscillation on P_{elec} originates from the PID flow modulation (reduced flow raises cell temperature and depresses electrical output, and vice versa).

Figure S4 reports the first-law efficiency timeline. The hybrid (electrical + thermal) efficiency settles around 52% during the productive window; the electrical share is suppressed to $\approx 9.3\%$ by the high PV cell temperature, while the thermal share reaches $\approx 43\%$. Figure S5 presents the corresponding daily energy distribution for the seed-1 realisation: of its 5.08 kWh of solar input, 0.47 kWh (9.3%) is converted to electricity, 2.19 kWh (43.0%) is recovered as useful thermal energy, and 2.42 kWh (47.6%) is lost (the $N = 30$ ensemble means are 5.14, 0.479 and 2.209 kWh; Table 6).

Figure S6 directly compares the two thermal-exergy formulations. The constant-temperature approximation $Q(1 - T_a/T_{\text{out}})$ returns a daily exergy yield of 0.821 kWh, whereas the rigorous Bejan/Kotas integrated form (Equation (12)) returns 0.671 kWh—a 22% overestimate by the simple formulation. The corresponding daily mean exergy efficiency drops from 17.5% to 14.3%. This is not a small correction and should be applied as default in future PVT studies where the per-pass temperature rise is comparable to $(T_{\text{in}} - T_a)$.

Figure S7 shows the time-resolved exergy decomposition under the corrected formulation. Electrical and thermal exergies are approximately equal in magnitude through the high-irradiance window, with the overall exergy efficiency η_{ex} (right axis) stable in the 14–18% band over the productive window.

Across a Monte-Carlo ensemble of $N = 30$ independent random seeds, the headline metrics are tightly clustered: daily exergy yield 0.678 ± 0.010 kWh (spanning 0.657–0.696 kWh across the 30 seeds); daily mean exergy efficiency $14.26 \pm 0.03\%$; time at setpoint 5.3 ± 0.3 h; total water throughput 41.6 ± 0.5 L (Table 6). The ensemble standard deviations are below 1.5% of the means; consistent with the modest sample, we report this spread as a robustness check rather than as a tight statistical confidence interval. The single-realisation (seed = 1) yield is 0.671 kWh under the rigorous formulation—the value against which the constant-temperature overestimate is computed ($0.821/0.671 \approx 1.22$, Section 4.1)—while the 0.678 kWh ensemble mean is the headline figure quoted throughout.

Table 6. Case 1 Monte-Carlo headline metrics ($N = 30$).

Metric	Mean $\pm \sigma$	Unit
Total solar energy input	5.14 ± 0.07	kWh
Electrical energy yield	0.479 ± 0.007	kWh
Thermal energy yield	2.209 ± 0.041	kWh
Exergy yield (rigorous, Equation (12))	0.678 ± 0.010	kWh
Daily mean exergy efficiency (gross, pump work excluded)	14.26 ± 0.03	%
Peak HTF outlet temperature	96.4 ± 0.3	°C
Peak PV cell temperature	103.6 ± 0.13	°C
Mean pump duty cycle	11.63 ± 0.15	%
Time at T_{sp} (within ± 2 K)	5.32 ± 0.25	h
Total water throughput	41.6 ± 0.5	L

4.2. Case 2—Step Disturbance and PID Recovery

Figure 8 shows the PID response to a 50% step reduction in G_{tilt} imposed at solar noon and held for 30 min. As the step is applied, T_7 drops by 11.6 K below the setpoint, reaching a minimum of 83.4 °C. On disturbance removal, T_7 recovers within 12.8 min to within 1 K of the setpoint, with a modest overshoot of 1.6 K—well within the +2 K tolerance band imposed by the TCES reaction-protection constraint.

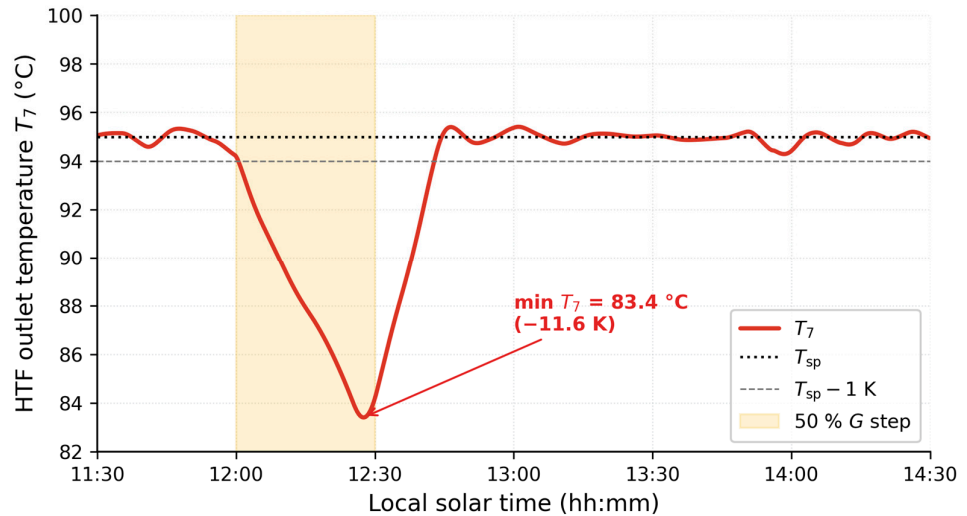


Figure 8. HTF outlet temperature T_7 during a 50% step reduction in G_{tilt} at solar noon, 30 min hold (Case 2). The disturbance causes a peak undershoot of 11.6 K; PID recovery to within 1 K of the setpoint is achieved in 12.8 min after the disturbance ends.

Figure 9 shows the corresponding pump flow response. The controller saturates at u_{min} during the step (when no actuation can recover the missing solar input) and then ramps the flow up smoothly on recovery.

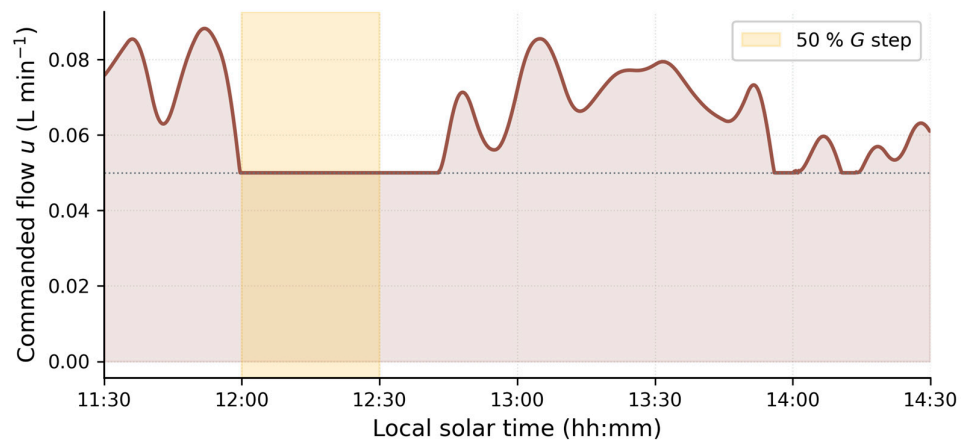


Figure 9. Commanded pump flow rate $u(t)$ during the step disturbance (Case 2): saturation at u_{min} during the window, smooth ramp-up on recovery.

Figure 10 reports the principal novelty metric: the cumulative exergy-delivery deficit during the disturbance and recovery transient (Equation (14)). Over the 42.8 min interval from disturbance onset to settling—the 30 min hold plus the 12.8 min recovery, the latter almost exactly one plant response time τ_{eff} (Section 3.3)—the integrated $\Delta \text{Ex}_{\text{deficit}}$ reaches

937 kJ. The dotted vertical line marks the end of the irradiance step ($t = 30$ min): the linearly growing cumulative curve before this point indicates that the deficit is dominated by the irrecoverable solar exergy lost during the irradiance reduction itself—when no PVT control action can recover the missing input. This finding has direct implications for which control architectures will and will not improve the result (Section 5.3).

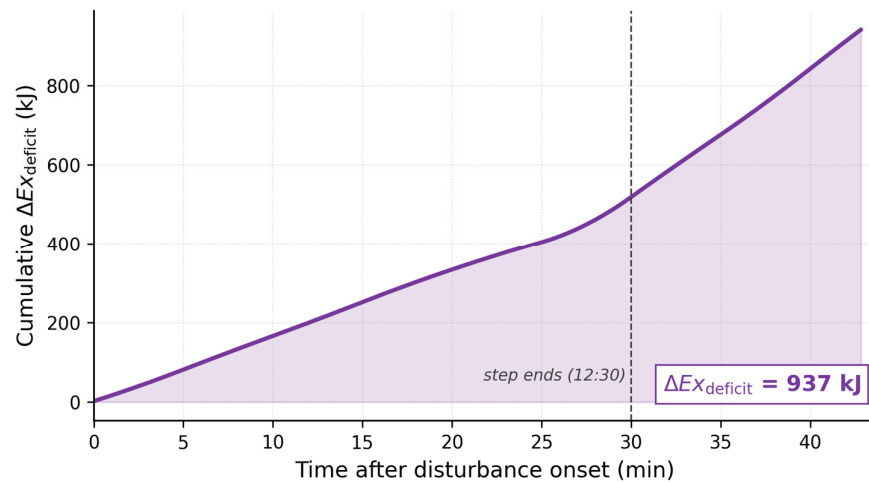


Figure 10. Cumulative exergy-delivery deficit $\Delta Ex_{\text{deficit}}$ during the step disturbance (Case 2, Equation (14)). The total reaches 937 kJ over the 42.8 min from disturbance onset to controller settling; the linear growth before $t = 30$ min shows that $\Delta Ex_{\text{deficit}}$ is dominated by the irrecoverable solar exergy during the disturbance, not by the recovery transient.

4.3. Case 3—PID Robustness to Sustained Irradiance Ramps

Figure 11 reports the PID response to sustained linear irradiance ramps imposed at solar noon over a 5 min window, with rates from -1 to $-5 \text{ W m}^{-2} \text{ s}^{-1}$. Despite the very wide range tested—the worst case drops the deterministic irradiance to zero within the 5 min window—the controller maintains the HTF outlet temperature within -0.4 K of the setpoint in every case, with no breach of the $T_{\text{sp}} - 1 \text{ K}$ reaction protection band. The minimum T_7 observed across all four ramps is $94.62 \text{ }^{\circ}\text{C}$, dominated by the cumulative stochastic background noise rather than the deterministic ramp.

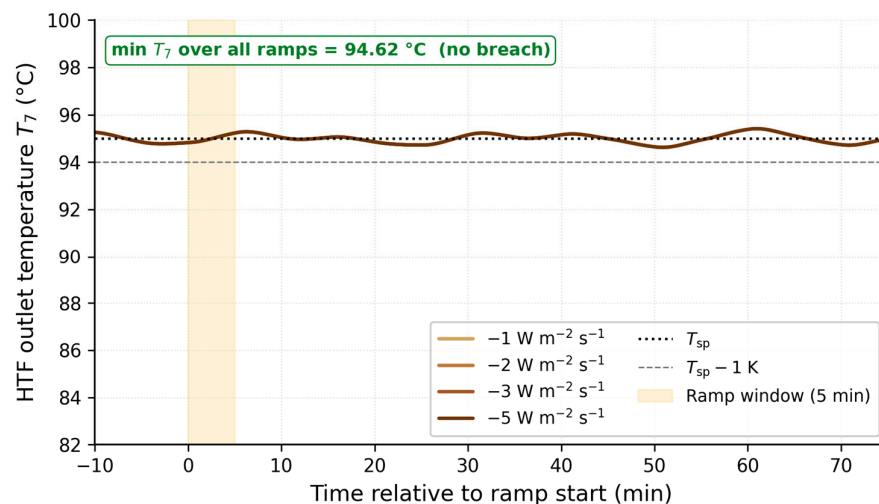


Figure 11. PID robustness to sustained linear irradiance ramps at noon (Case 3), each over a 5 min window. In every case the minimum HTF outlet is $94.62 \text{ }^{\circ}\text{C}$, well above the $T_{\text{sp}} - 1 \text{ K}$ reaction

protection band. The system is structurally robust to single-cloud-passage transients at the present collector scale.

The robustness has a two-part physical explanation: (i) the collector thermal mass (≈ 3 kg of copper absorber, node ⑥, $m_{6C6} \approx 1.16$ kJ K⁻¹, plus the 0.8 L water inventory, node ⑦, $m_{7C7} \approx 3.34$ kJ K⁻¹, thermally backed by the glazing stack; Table 3) buffers the immediate effect of the ramp on T_7 —the plant’s effective input-to-outlet response time is $\tau_{\text{eff}} \approx 13$ –15 min (Section 3.3), far longer than the 5 min ramp window; (ii) the controller throttles to u_{min} within 10 s of detecting the descending error, preserving the heat-carrying capacity of the residual collected energy.

4.4. Case 4—PID Vs. Fixed-Flow Operation

Figure 12 compares the PID-controlled HTF outlet temperature against four fixed-flow benchmarks ($u = 0.5, 1.0, 2.0, 5.0$ L min⁻¹) under identical stochastic irradiance forcing; the three highest rates exceed the controlled pump’s own $u_{\text{max}} = 0.5$ L min⁻¹ and represent alternative fixed-speed design points typical of conventional PVT practice, not the modelled actuator. The contrast is stark: under PID control the outlet reaches the 95 °C TCES threshold and remains within ± 2 K of it for approximately 5.3 h; under any of the fixed-flow benchmarks, the outlet temperature never exceeds 48 °C. Table 7 quantifies this.

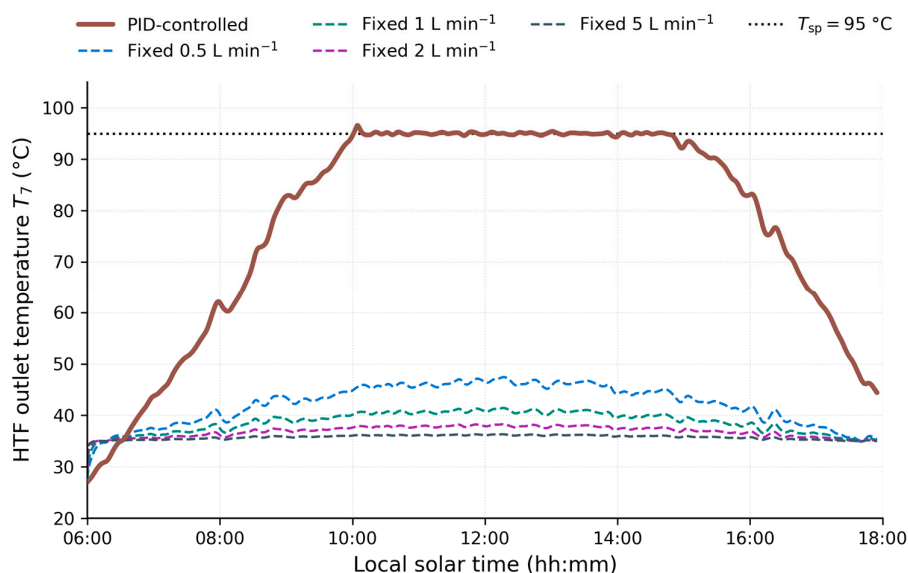


Figure 12. PID-controlled (brown) versus fixed-flow operation at four representative flow rates (Case 4). The PID controller reaches and maintains the 95 °C TCES setpoint; no fixed-flow operating point in the tested range exceeds 48 °C, leaving the SrCl₂/NH₃ reactor 47 K below its desorption threshold throughout the day.

Table 7. Comparison of PID and fixed-flow operation across four representative flow rates (single seed-1 realisation; the ensemble-mean peak outlet under PID is 96.4 ± 0.3 °C, Table 6).

Control Mode	Flow Rate (L min ⁻¹)	PeakT _{out} (°C)	PeakT _{pv} (°C)	Time at T _{sp} (h)
PID-controlled	0.05–0.10 (auto)	96.6	103.6	≈ 5.3
Fixed flow	0.5	47.4	57.0	0
Fixed flow	1.0	41.4	51.3	0
Fixed flow	2.0	38.3	48.2	0
Fixed flow	5.0	36.3	46.4	0

Even at the lowest physically reasonable fixed rate (0.5 L min^{-1}), the pump removes thermal energy from the absorber faster than the 0.6834 m^2 aperture can supply it. The per-pass temperature rise is limited to $\Delta T \approx 12 \text{ K}$ at solar noon, leaving the outlet $\approx 47 \text{ K}$ below the $\text{SrCl}_2/\text{NH}_3$ desorption threshold; no TCES discharge can occur in any of the fixed-flow cases. The PID controller, by throttling the flow down to $0.05\text{--}0.10 \text{ L min}^{-1}$, forces a per-pass rise of more than 60 K and delivers heat at the required temperature.

4.5. Case 5—Gain Sensitivity

Figures 13–15 report the response of the three principal step-disturbance metrics to a 4×4 grid of (K_p, K_d) gains— $K_p \in [0.005, 0.040]$ ($0.33\text{--}2.7\times$ nominal, an 8-fold span) and $K_d \in [0.05, 1.00]$ ($0.25\text{--}5\times$ nominal, a 20-fold span)—with K_i held at the nominal value. Three findings emerge.

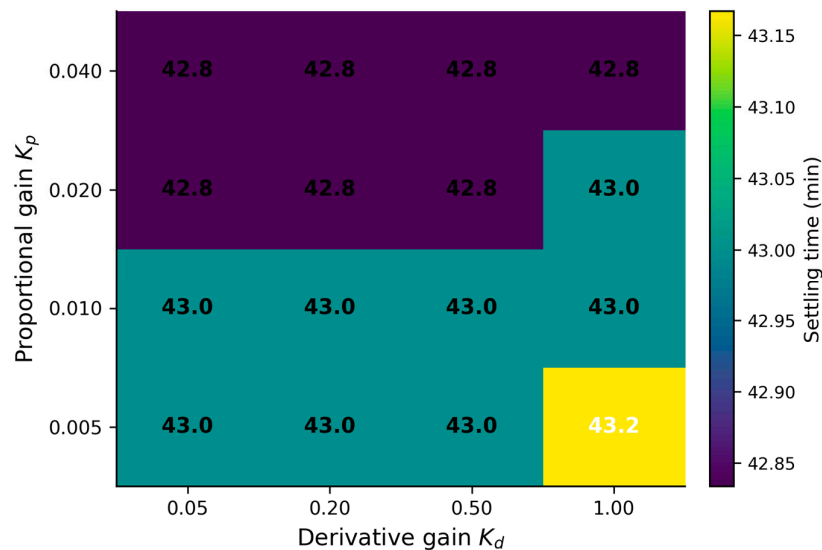


Figure 13. PID gain sensitivity—settling time (Case 5).

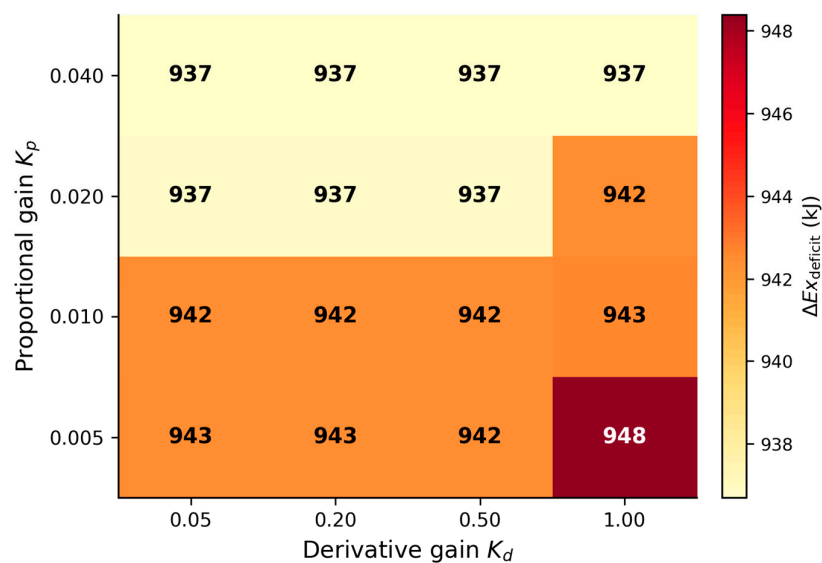


Figure 14. PID gain sensitivity—accumulated exergy delivery deficit $\Delta Ex_{\text{deficit}}$ (Case 5).

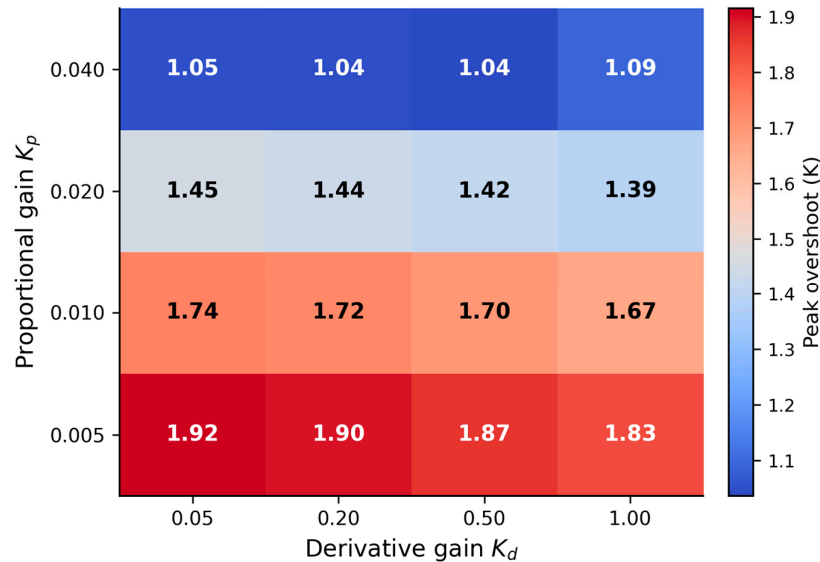


Figure 15. PID gain sensitivity—peak overshoot (Case 5).

First (Figure 14), the accumulated exergy delivery deficit $\Delta Ex_{\text{deficit}}$ is remarkably insensitive to gain choice—it varies from 937 to 948 kJ across the entire grid, a span of only 1.3%. This is because $\Delta Ex_{\text{deficit}}$ is dominated by the irrecoverable solar exergy lost during the 30 min irradiance reduction itself, with only a small additional contribution from the recovery transient. Reducing $\Delta Ex_{\text{deficit}}$ substantially below the 937 kJ bound therefore requires anticipatory control (i.e., MPC with an irradiance forecast), not tighter PID tuning.

Second (Figure 15), the peak overshoot exhibits a clear monotonic trend with K_p , decreasing from 1.9 K at $K_p = 0.005$ to 1.0 K at $K_p = 0.040$. K_d has negligible effect on overshoot in this regime. The nominal $K_p = 0.015$ therefore sits in the lower-overshoot half of the range; if a tighter overshoot envelope is required for reaction protection reasons, K_p can be increased to 0.04 with no penalty in $\Delta Ex_{\text{deficit}}$.

Third (Figure 13), the settling time—the 30 min imposed hold plus the physical recovery—is essentially flat at 42.8–43.2 min across the entire grid: the ≈ 12.8 min recovery is almost exactly one plant response time $\tau_{\text{eff}} \approx 13$ min (Section 3.3), i.e., it is rate-limited by the collector's thermal masses rather than by the controller dynamics. Faster recovery would therefore require either reducing the thermal mass or providing anticipatory control.

4.6. Summary of Headline Results

Table 8 collates the principal numerical results across the five cases (synthetic stochastic forcing; the corrected exergy formulation, Equation (12), is used throughout). For the measured-day counterparts see Section 6: the measured clear-sky day (23 February 2026, $K_t = 0.52$) yields $E_{\text{tot}} = 1.384$ kWh, $Ex_{\text{tot}} = 0.434$ kWh, $\eta_I = 38.1\%$ and $\psi_{II} = 12.9\%$. The two scenarios' exergy efficiencies agree closely (14.3% vs. 12.9%), while the first-law figures differ ($\approx 52\%$ vs. 38.1%) because the moderate-clearness measured day captures less useful thermal energy per unit of aperture input than the higher-clearness synthetic days—a difference in forcing, not an inconsistency.

Table 8. Principal numerical results across the five case studies.

Metric	Value	Unit
Case 1: daily exergy yield (Bejan/Kotas, $N = 30$)	0.678 ± 0.010	kWh

Case 1: daily mean exergy efficiency (gross, pump work excluded)	14.26 ± 0.03	%
Case 1: electrical energy yield	0.479 ± 0.007	kWh
Case 1: thermal energy yield	2.209 ± 0.041	kWh
Case 1: time at $T_{sp} = 95\text{ }^{\circ}\text{C} \pm 2\text{ K}$	5.32 ± 0.25	h
Case 1: peak PV cell temperature	103.6 ± 0.13	$^{\circ}\text{C}$
Case 1: mean pump duty cycle	11.6 ± 0.15	%
Case 2: settling time (1 K)	12.8	min after step end
Case 2: peak undershoot below T_{sp}	11.6	K
Case 2: peak overshoot above T_{sp}	1.6	K
Case 2: $\Delta Ex_{\text{deficit}}$ (Equation (14))	937	kJ
Case 3: $\min T_{\text{out}}$ under sustained $5\text{ W m}^{-2}\text{ s}^{-1}$ ramp	94.62 (no breach)	$^{\circ}\text{C}$
Case 4: time at T_{sp} for fixed flow $\geq 0.5\text{ L min}^{-1}$	0	h
Case 5: $\Delta Ex_{\text{deficit}}$ sensitivity to gain	937–948 (1.3% span)	kJ

5. Discussion

5.1. PID as Enabling Control—Not Optimiser

The clearest finding of this study is that closed-loop flow rate control is a structural enabling condition for PVT–TCES integration, not a performance optimisation. Case 4 (Section 4.4) shows that no fixed-flow operating point in the $0.5\text{--}5.0\text{ L min}^{-1}$ range can sustain outlet temperatures within 47 K of the $\text{SrCl}_2/\text{NH}_3$ desorption threshold. This is a structural consequence of the heat capacity of water and the available solar power per square metre: a per-pass temperature rise of 60 K is only achievable at flow rates an order of magnitude below the typical PVT design point. Without an active controller to throttle the pump into this regime, the integrated system cannot operate as designed. This reframes PID—and any successor controller—from a peripheral optimisation choice to a primary architectural component of any PVT–TCES installation.

5.2. PID Is Robust to Realistic Cloud Transients

Case 3 (Section 4.3) demonstrates that the controller maintains the HTF outlet within -0.4 K of the setpoint under sustained ramps as severe as $-5\text{ W m}^{-2}\text{ s}^{-1}$ over 5 min—the worst case is dominated by stochastic background noise rather than the deterministic ramp. The robustness is structural: the absorber thermal mass and rapid throttling to u_{min} together render the system insensitive to single-cloud-passage events at the present collector scale. The implication is that a properly tuned PID with conditional-integration anti-windup is sufficient to protect the TCES reaction window against the typical cloud transients of tropical–coastal climates.

5.3. The Reactive-Control Penalty: $\Delta Ex_{\text{deficit}}$ Has a Controller-Independent Bound

Case 2 (Section 4.2) quantifies the principal novelty metric: $\Delta Ex_{\text{deficit}} = 937\text{ kJ}$ over the 30 min step disturbance and the 12.8 min recovery. The gain sensitivity analysis (Case 5, Section 4.5) shows that this value cannot be reduced substantially below $\approx 937\text{ kJ}$ by any reasonable PID tuning—the entire 4×4 gain grid lies within 1.3% of the nominal value. $\Delta Ex_{\text{deficit}}$ is dominated by the irrecoverable solar exergy that the system cannot deliver during the irradiance reduction itself, not by the recovery overshoot: the controller is by construction blind during the initial 60–90 s of the disturbance, and the integral of unmet exergy demand during this window dominates the cumulative metric. Because the figure is set by the disturbance and the plant, not by the controller, it is a controller-independent benchmark for this PVT–TCES configuration and the rigorously characterised feedback-only lower bound.

We are explicit that this benchmark is a property of the plant as well as of the disturbance: re-deriving it on the jointly validated shared plant—which corrects a non-conservative back-insulation coupling and re-aligns the absorber time constants relative to the original per-paper model—raises it from 745 kJ on the uncorrected model to 937 kJ, while leaving its controller-independence intact (a 1.3% span across the gain sweep, against 1.5% before). The value is therefore ‘structural’ in the precise and defensible sense that it is invariant to controller tuning, not in the sense of being independent of the plant; we report it on the validated plant. That plant is no longer a purely synthetic construct: its thermal core reproduces measured rooftop module temperatures at the study site (Section 3.6; RMSE 3.8 °C, $R^2 = 0.94$, $n = 170$); its shared eight-node formulation passes the 26-test verification suite summarised in Appendix A.1; and its active water loop is realised in a co-located, PID-controlled PVT prototype, so the 937 kJ benchmark is anchored to measured physics rather than to model idealisation.

A model predictive controller fed by a short-horizon irradiance forecast can pre-position the flow before the disturbance propagates to T_7 , reducing the under-delivered exergy; the 937 kJ value is the worst-case, feedback-only benchmark against which any such anticipatory architecture is judged—a lower bound over PID tunings, yet the maximum that anticipation improves upon—and the value of anticipation is precisely the distance by which predictive control falls below it; on this plant and under the measured monsoon forcing of Section 3.6, companion studies (Korkua et al., under review) find this to be of order 1% or less. Indeed, on the two measured Walailak University days (a smoother day, 24 September 2024, clearness index $K_t = 0.26$; and a more intermittent day, 10 September 2024, $K_t = 0.37$; Table 5) the PID baseline delivers 0.190 and 0.286 kWh of daily exergy, and the predictive controllers add only 0.07–0.24%, because the measured plane-of-array irradiance rarely lifts the outlet to the 95 °C setpoint (regulation-window tracking RMSE of 6–13 K for all three controllers). The practical implication is that, for PVT–TCES at this scale and latitude, the decisive lever is deployability—a controller that captures the available exergy at embedded-systems cost—rather than the small exergy margin that anticipation buys.

5.4. The Corrected Exergy Formulation Matters

Figure S6 shows that the simplified constant-temperature exergy form $Q \cdot (1 - T_a/T_{out})$, widely used in earlier PVT exergy work, overestimates the daily exergy yield by about 22% relative to the rigorous Bejan/Kotas integrated form for the present configuration. For a PVT–TCES configuration where the HTF temperature swing across the heat exchanger is comparable to $(T_{in} - T_a)$, this correction is essential. Future studies of PVT exergy involving significant per-pass temperature rises should adopt the Bejan/Kotas form by default.

5.5. Position Relative to the Existing Literature

The daily mean exergy efficiency of 14.3% reported here (the synthetic 30-seed ensemble value; the measured clear-day counterpart is 12.9%, Section 6), computed with the rigorous formulation, is at the lower end of the 14–22% range reported for PVT collectors by Fudholi et al. [20] and in the studies surveyed by Şirin et al. [22]. The lower position reflects two effects: (a) the simultaneous TCES-threshold constraint (95 °C), which forces the PV cell into a 100 °C-class temperature regime and incurs the ≈ 6.5 pp electrical-efficiency penalty; and (b) the rigorous exergy formulation, which reduces all reported values by $\approx 20\%$ relative to the constant-T approximation. The 25–29% steady-state values reported by the geothermal-based PID exergy studies [13,15,23] are not directly comparable, as those systems operate against a thermally stable input source.

5.6. Limitations

(i) The TCES reactor is represented by a setpoint constraint; explicit $\text{SrCl}_2/\text{NH}_3$ reaction kinetics from the previously published reactor model [10] are not coupled to the PVT model here. Doing so is planned future work. (ii) As detailed in Section 3.6, the experimental grounding validates the collector's thermal behaviour but not its electrical model: the prototype inlet temperature was not logged and the panel was not at its maximum-power point, so $\eta(T)$ rests on the manufacturer datasheet, the validation covers panel/absorber heat transfer rather than the full once-through rise, and one module loss coefficient is site-calibrated (standard NOCT/Faiman practice). (iii) The stochastic irradiance is a Gaussian additive noise model on a deterministic envelope; it does not capture the long-range correlation structure of real cloud-shadow time series [30]. A wavelet-based variability model is planned for a follow-up multi-day study. (iv) The Monte-Carlo ensemble is $N = 30$ seeds; the spread (below 1.5% of the mean) is reported as a robustness range rather than as a tight statistical confidence interval. (v) Pump electrical consumption is neglected, so all reported exergy efficiencies are gross values (Section 3.4). This is justified by the low daily mean duty cycle (11.6%) and the ultra-low-flow band—the pumping penalty is at most of order 1% of the daily exergy yield—so the net-versus-gross correction does not affect any conclusion; pump work should nonetheless be included in any techno-economic extension. (vi) The pump is modelled as an ideal flow source bounded to $[0.05, 0.50]$ L min^{-1} with no head-flow or efficiency curve; the deployability conclusion presumes a pump capable of stable delivery in the 0.05–0.10 L min^{-1} band. Pump minimum-stable-flow/turndown (and, for centrifugal designs, low-flow instability) is the governing hardware constraint and should be verified against the selected pump in any deployment; the WU prototype (Section 3.6) demonstrates feasibility with a small 12 V DC circulation pump, and a full pump-curve characterisation is left to future techno-economic work.

6. Energy and Exergy Output Under Contrasting Measured Days

To characterise the first- and second-law output of the controlled collector beyond the validation days, the validated plant was driven by measured Walailak University data for two contrasting days—a clear-sky day (23 February 2026, $K_t = 0.52$) and an overcast day (7 May 2024, $K_t = 0.13$); both are drawn from the three-year rooftop record and are distinct from the September 2024 validation days of Section 3.6 (Table 5). For each day the electrical yield E_{el} , the useful thermal yield E_{th} and their sum E_{tot} were evaluated, with the corresponding exergies: electrical exergy equals electrical energy (electricity is pure exergy), Ex_{th} follows the Bejan/Kotas form of Equation (12), and $Ex_{tot} = E_{el} + Ex_{th}$; η_I and ψ_{II} are the first-law efficiency and the Petela-referenced (Equation (13)) exergy efficiency.

On the sunlit day the thermal stream dominates the energy output while the electrical stream dominates the exergy output: although the thermal energy is roughly three times the electrical energy, the $\approx 95^\circ\text{C}$ heat carries an exergetic quality factor (Carnot factor, $1 - T_a/T_7$, at the instantaneous ambient dead state) of only ≈ 0.18 —and an integrated Bejan/Kotas quality Ex_{th}/E_{th} of only ≈ 0.09 —so the total exergy is less than one-third of the total energy and is about 80% electrical (Figure 16; Table 9). On the overcast day the controller holds the pump nearly closed because the outlet cannot reach the desorption threshold, so the thermal output collapses to essentially zero and the collector delivers only electricity ($E_{tot} = Ex_{tot} = 0.10$ kWh); the clear-sky day delivers 1.38 kWh of energy but only 0.43 kWh of exergy.

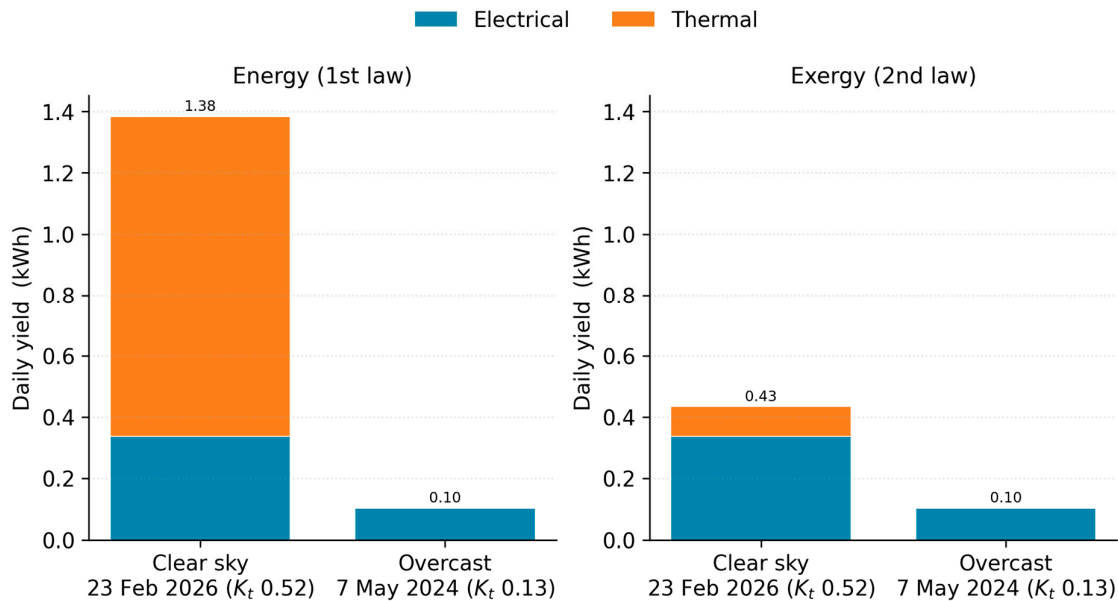


Figure 16. Daily electrical, thermal and total output in energy (first-law) and exergy (second-law) terms, driven by measured Walailak University data (clear-sky day 23 February 2026, K_t 0.52; overcast day 7 May 2024, K_t 0.13). Electrical exergy equals electrical energy.

Table 9. First- and second-law daily output (kWh) and efficiencies. E_{el} , E_{th} , E_{tot} : electrical, thermal and total energy; Ex_{th} , Ex_{tot} : thermal and total exergy (electrical exergy = E_{el}). η_I , first-law efficiency; ψ_{II} , gross exergy efficiency (Petela reference; pump work excluded).

Day	Controller	E_{el}	E_{th}	E_{tot}	Ex_{th}	Ex_{tot}	η_I (%)	ψ_{II} (%)
Clear sky	PID	0.338	1.046	1.384	0.096	0.434	38.1	12.9
Overcast	PID	0.104	0.000	0.104	0.000	0.104	11.7	12.6

7. Conclusions and Future Work

7.1. Conclusions

This paper has presented, to our knowledge, the first dynamic response analysis of PID flow rate control in a solar PVT collector charging a $\text{SrCl}_2/\text{NH}_3$ thermochemical energy storage reactor under intermittent solar irradiance and has used it to establish a rigorously characterised feedback-only performance benchmark for the configuration. An eight-layer lumped-capacitance thermal model is coupled to a discrete-time PID controller with conditional-integration anti-windup and exercised through five case studies. The eight-layer plant model underlying these results is partially validated against measured data from the same tropical-coastal site (Section 3.6), so the conclusions below rest on a measurement-grounded plant. We emphasise that the validation is partial (thermal core and absorber-to-water heat transfer; the electrical model and the full once-through temperature rise are not experimentally closed) and that the 95 °C setpoint operation, the ≈ 5.3 h charging window, all reported exergy yields and the 937 kJ deficit are simulation results: the built prototype reached a peak outlet of 79 °C, not the 95 °C setpoint. The principal conclusions are:

(1) Under the adopted gain set ($K_p = 0.015$, $K_i = 2 \times 10^{-4}$, $K_d = 0.20$) and a 10 s control update interval, the HTF outlet temperature is held within +1.4 K of the 95 °C TCES desorption setpoint for 5.3 ± 0.3 h around solar noon, despite stochastic irradiance perturbations of $\sigma = 120 \text{ W m}^{-2}$.

(2) No fixed-flow operating point in the 0.5–5.0 L min^{−1} range can sustain outlet temperatures within 47 K of the TCES setpoint; closed-loop flow control is therefore a structural enabling condition for PVT–TCES integration.

(3) Following a 50% step reduction in irradiance held for 30 min, the accumulated exergy delivery deficit is $\Delta E_{\text{deficit}} = 937$ kJ (Equation (14)), evaluated on the jointly validated plant. This is, to our knowledge, the first published value of this metric for a PVT–TCES system and, because it is set by the disturbance and the plant rather than by the controller, it serves as a reusable, controller-independent benchmark against which any predictive controller for this configuration must be evaluated.

(4) PID gain sensitivity over the 4×4 grid (K_p 0.33–2.7 $\times$, K_d 0.25–5 $\times$ of nominal) changes $\Delta E_{\text{deficit}}$ by only 1.3%, demonstrating that the metric is limited by the disturbance itself—not by controller tuning. Substantial reductions therefore require anticipatory control, not tighter PID gains.

(5) The controller is robust to sustained linear irradiance ramps as severe as -5 W m^{−2} s^{−1} over 5 min; the minimum HTF outlet temperature in all tested ramp cases is 94.62 °C, well above the $T_{\text{sp}} - 1$ K reaction protection band.

(6) The TCES-protective operating mode incurs a structural electrical-efficiency penalty of ≈ 6.5 pp relative to STC, attributable to the elevated PV cell temperature (peak 103.6 °C) required to maintain the setpoint.

(7) The rigorous Bejan/Kotas exergy formulation gives a daily mean exergy efficiency of $14.3 \pm 0.03\%$ and a total exergy yield of 0.678 ± 0.010 kWh (30-seed ensemble)—values that correct a +22% overestimate in the constant-T approximation and should serve as the corrected reference for future PVT–TCES studies.

(8) The eight-layer plant model is partially validated at the study site: its thermal core reproduces measured rooftop PV module temperature with an RMSE of 3.8 °C ($R^2 = 0.94$, $n = 170$), and a purpose-built, PID-controlled glazed copper-tube PVT prototype validates the absorber-to-water heat transfer (panel-temperature RMSE 1.3 °C; copper-consistent conductance $UA_f \approx 85$ W m^{−2} K^{−1}) while delivering hot water with a peak outlet up to 79 °C (daily maxima 46–79 °C; daily means 39–56 °C) under monsoon skies. The 937 kJ benchmark and the PID baseline therefore rest on a plant grounded in measured physics under real tropical forcing rather than on a purely synthetic model. A companion comparison on the same plant shows that anticipatory and model-free predictive controllers recover at most about 1% of additional exergy under synthetic forcing and 0.07–0.24% on measured monsoon days, so the practical lever for PVT–TCES at this scale is deployability rather than control sophistication.

7.2. Future Work

The controller-independent $\Delta E_{\text{deficit}}$ bound of 937 kJ identified in Case 5 motivates a planned extension to model predictive control (MPC). An MPC implementation tailored to this system would ingest a short-horizon irradiance forecast and pre-position the flow rate during the approach of a cloud shadow. The expected improvement is structural—reducing the irrecoverable component of $\Delta E_{\text{deficit}}$ by anticipating the disturbance rather than reacting to it—and the magnitude is quantified in companion studies (Korkua et al., under review) that use the present 937 kJ value as their reference anchor. Three additional extensions are planned: (i) closing the full dynamic water-loop validation with logged inlet temperature and maximum-power-point electrical measurements, extending the thermal-core and prototype validation of Section 3.6; (ii) explicit coupling with the dynamic SrCl₂/NH₃ reactor model of [10]; and (iii) a multi-day simulation across seasonal irradiance archetypes.

Supplementary Materials: The following supporting information can be downloaded at: <https://www.mdpi.com/article/10.3390/en19143436/s1>, The following supporting figures, computed on the jointly validated plant under a representative stochastic day (seed 1), are provided separately: Figure S1 (Case 1 temperature tracking), Figure S2 (commanded pump flow), Figure S3 (POA irradiance and PV electrical power), Figure S4 (first-law efficiency timeline), Figure S5 (daily energy distribution), Figure S6 (constant-temperature versus Bejan/Kotas exergy), Figure S7 (exergy decomposition), and Listings S1–S4 (reference code listings: Erbs diffuse fraction, eight-layer ODE function, PID with anti-windup, and Bejan/Kotas exergy).

Author Contributions: Conceptualisation, K.T., S.K.K., and C.R.; methodology, K.T., S.K.K., and C.R.; software, K.T.; validation, K.T. and M.F.N.b.T.; formal analysis, K.T., S.K.K., and C.R.; investigation, K.T., K.F., S.K.K., and C.R.; resources, S.K.K., C.R., and K.T.; data curation, K.T., S.K.K., and C.R.; writing—original draft preparation, C.R., K.T., and S.K.K.; writing—review and editing, S.K.K., C.R., M.F.N.b.T., S.K.D., and K.T.; visualisation, K.T. and K.F.; supervision, K.T., S.K.K., C.R., and M.F.N.b.T.; project administration, S.K.K., C.R., and K.T.; funding acquisition, K.T. All authors have read and agreed to the published version of the manuscript.

Funding: This work was partially supported by Walailak University under the International Mobility for Publication and Collaboration scheme (Contract Number WU-CIA-07408/2025).

Data Availability Statement: The simulation code (the shared plant model `pvt_plant.py` and the controller script `pvt_tces_pid_corrected.py`), the controller-comparison and figure-generation scripts that reproduce all results and figures, and the measured Walailak University rooftop irradiance and temperature record and the PVT-prototype logs used for the validation of Section 3.6, together with the validation scripts, are openly available in the Zenodo public repository at <https://doi.org/10.5281/zenodo.20644161> (reference number 20644161).

Acknowledgments: The revised manuscript was proofread by the language service of Walailak University. During the preparation of this manuscript, the authors additionally used Claude (Anthropic; models Claude Opus 4.8 and Claude Fable 5) to improve the language and check the readability of the text. The authors have reviewed and edited all output and take full responsibility for the content of this publication.

Conflicts of Interest: The authors declare no conflicts of interest.

Nomenclature

Symbol	Definition	Value/unit
A	collector aperture area (modelled reference collector)	0.6834 m ²
A_{tube}	tube–fluid contact area	0.41 m ²
c_i, m_i	specific heat/mass of node i (Table 3)	J kg ^{−1} K ^{−1} , kg
$c_{p,w}$	specific heat of water	4180 J kg ^{−1} K ^{−1}
d_f	Erbs diffuse fraction (Equation (3))	–
DHI, BHI	diffuse/beam horizontal irradiance (Equation (3a))	W m ^{−2}
$e(k)$	outlet-temperature error at control step k (Equation (10))	K
$\dot{E}X_{\text{solar}}, \dot{E}X_{\text{elec}}, \dot{E}X_{\text{th}}$	solar/electrical/thermal exergy rates (Equations (13a), (13b) and (12))	W
$\Delta EX_{\text{deficit}}$	accumulated exergy delivery deficit (Equation (14))	kJ
$\dot{E}X_{\text{loss}}, \dot{S}_{\text{gen}}$	loss exergy rate; entropy generation rate (control-volume balance, Section 3.4)	W; W K ^{−1}
G_{clear}	clear-sky global horizontal irradiance (Equation (1))	W m ^{−2}
G_h	global horizontal irradiance, GHI (Equation (2))	W m ^{−2}
G_{tilt}	plane-of-array irradiance (Equation (3b))	W m ^{−2}
G_{sc}	solar constant	1367 W m ^{−2}
h_{cond}	inter-layer (solid) conduction coefficient	200 W m ^{−2} K ^{−1}
$h_{\text{conv}}(v)$	wind-dependent front convection, McAdams (Equation (9))	W m ^{−2} K ^{−1}
h_{fluid}	tube-to-fluid convection coefficient	350 W m ^{−2} K ^{−1}
h_{gap}	air-gap conduction coefficient	5.5 W m ^{−2} K ^{−1}
h_{ins}	PU-foam back-loss coefficient	0.83 W m ^{−2} K ^{−1}

h_{rad}	sky radiation coefficient	$5.0 \text{ W m}^{-2} \text{ K}^{-1}$
j, k	summation index (0...k); current discrete control step	—
k_t, K_t	instantaneous/daily clearness index	—
K_p, K_i, K_d	PID gains	0.015; 2×10^{-4} ; 0.20
$\dot{m}$	HTF mass flow rate	kg s^{-1}
n_d	day of year	81
P_{elec}	PV electrical power (Equation (5))	W
Q_{th}	useful thermal heat removal rate (Equation (8))	W
R_b	beam tilt (transposition) factor (Equation (3b))	—
$t_{\text{dist}}, t_{\text{settle}}$	disturbance onset; settling time (Equation (14))	s
T_i ($T_1 \dots T_8$)	node temperatures, nodes ①–⑧ (T_7 = HTF outlet)	$^{\circ}\text{C}$
$T_a(t)$	ambient temperature = exergy dead state (instantaneous)	$^{\circ}\text{C}$
T_{in}	HTF inlet temperature (municipal once-through feed)	35°C
T_{ref}	STC reference temperature (PV model)	25°C
T_{sp}	PID setpoint (TCES desorption threshold)	95°C
$T_{\odot}$	effective sun temperature (Petela, Equation (13))	5778 K
$u; u_{\text{min}}, u_{\text{max}}$	commanded pump flow; bounds	L min^{-1} ; 0.05, 0.50
v	wind speed	m s^{-1}
VI	variability index	—
$\alpha_{\text{glass}}, \alpha_{\text{pv}}$	cover-glass/PV-cell absorptance	0.04; 0.90
β	PV temperature coefficient (Equation (4))	0.0045 K^{-1}
β_t	collector tilt angle	10°
$\delta, \omega, \theta, \theta_z, \varphi$	solar declination, hour angle, incidence & zenith angles, site latitude	deg ($\varphi = 8.643^{\circ} \text{ N}$)
Δt	control interval	10 s
$\eta(T_4), \eta_{\text{ref}}$	PV electrical efficiency; STC reference value (Equation (4))	—; 0.16
η_I	first-law (energy) efficiency	—
Q	ground albedo	0.20
σ_G	irradiance-noise standard deviation (Equation (2))	120 W m^{-2}
$\tau_{\text{cov}}, \tau_{\text{mod}}$	cover-/module-glass transmittance	0.91; 0.92
τ_{eff}	effective 63% input-to-outlet response time (Section 3.3)	$\approx 13\text{--}15 \text{ min}$
$\psi(T_a)$	Petela solar-exergy factor (Equation (13))	—
$1 - T_a(t)/T_7$	Carnot factor (exergetic quality factor) of heat delivered at the outlet temperature T_7 (Abstract, Section 6)	—
ψ_{II}	gross exergy (second-law) efficiency	—
$\mathcal{N}(\mu, \sigma^2)$	Gaussian (normal) random draw of mean μ , variance σ^2	—
$\text{clip}[x, a, b]$	clamp of x to the interval $[a, b]$	—

Appendix A. Cross-Controller Comparison on the Jointly Validated Plant

This appendix reports the three-controller comparison—the present PID baseline against a perfect-foresight predictive controller and a model-free predictive controller—on the jointly validated shared plant under synthetic clear and intermittent days, complementing the PID-focused analysis of the main text. The verification of that shared plant and the measured-day results that anchor the deployability discussion are summarised in Section 5.3.

The PID baseline of this study is re-evaluated here on the unified plant model that underpins the wider controller comparison, so that its feedback-only performance is expressed on exactly the same footing as the predictive schemes. The PID retains its role as the closed-loop reference: it tracks the charging setpoint with the largest regulation error of the three controllers on both irradiance regimes, while delivering the lowest daily exergy.

Appendix A.1. Validation of the Shared Plant

The shared eight-node lumped-capacitance plant passed a 26/26 verification suite: global energy balance closure to machine precision (1.3×10^{-13} relative), time-step independence, monotone night-time relaxation to ambient, a temperature-dependent PV efficiency that decreases monotonically with cell temperature, agreement between the long-time integration and the exact linear steady-state solution to within 0.02 K, the expected limiting behaviour (outlet temperature approaching the inlet as flow grows, and approaching the absorber temperature as the fluid conductance grows), and non-negative thermal exergy bounded above by the thermal energy. The verification also uncovered and corrected a non-conservative back-insulation coupling in the original model, in which the back node received conduction from the absorber without the absorber losing it.

Appendix A.2. Controller Comparison Under Clear and Intermittent Skies

Re-running the PID baseline alongside the predictive controllers on a clear and an intermittent day isolates how much of its error is intrinsic to feedback-only action. On the clear day, the PID holds the outlet within a few kelvin of setpoint; under intermittency its regulation error grows, and the gap to the predictive schemes widens.

The two regimes are defined quantitatively rather than by eye: the low-variability ('clear') day has a daily clearness index $K_t = 0.74$ with variability index $VI = 1.03$ and no minutes exceeding the ramp threshold, whereas the high-variability ('intermittent') day has $K_t = 0.72$, $VI = 1.27$ and a ramp fraction of 0.06—the two synthetic archetypes are matched in clearness and differ in variability (Table 5). Table A1 reports the daily exergy, tracking error and pump duty for the three controllers on both synthetic days; the perfect-foresight and model-free predictive controllers improve on the PID by 0.96% and 0.98% (clear) and 0.96% and 0.97% (intermittent), confirming that feedback-only performance sits only marginally below the anticipatory ceiling on this plant.

Table A1. Cross-controller comparison on the jointly validated shared plant under the synthetic clear and intermittent days. The time-at-setpoint values here (7.2–8.8 h, ± 2 K band, synthetic archetype days) are not comparable with the 5.3 ± 0.3 h of Table 6, which is the mean over the $N = 30$ stochastic Monte-Carlo days of Section 4.1; the forcing days differ.

Day	Controller	Daily Exergy (kWh)	ΔEx vs. PID (%)	Tracking RMSE (K)	Mean Duty (%)	Time at Setpoint (h)
Clear	PID	0.6487	+0.00	2.68	8.00	7.59
Clear	MPC	0.6549	+0.96	2.33	7.78	8.79
Clear	MFPC	0.6550	+0.98	2.38	7.78	8.75
Intermittent	PID	0.6294	+0.00	3.35	7.67	7.19
Intermittent	MPC	0.6354	+0.96	3.10	7.45	8.37
Intermittent	MFPC	0.6354	+0.97	3.12	7.46	8.35

Appendix B. The Complete Eight-Node Energy Balance System

Each node i of Figure 1 (nodes ①–⑧) obeys a lumped balance $m_i c_i dT_i/dt = \Sigma U A (T_j - T_i) + S_i$, with the heat transfer coefficients of Table 2 and the capacitances of Table 3. Written out—transcribed directly from the released implementation (pvt_plant.py)—the eight balances are:

$$m_1 c_1 \frac{dT_1}{dt} = \alpha_{\text{glass}} G_{\text{tilt}} A - (h_{\text{conv}}(v) + h_{\text{rad}}) A (T_1 - T_a) - h_{\text{gap}} A (T_1 - T_2) \quad (\text{A1})$$

$$m_2 c_2 \frac{dT_2}{dt} = h_{\text{gap}} A (T_1 - T_2) - h_{\text{gap}} A (T_2 - T_3) \quad (\text{A2})$$

$$m_3 c_3 \frac{dT_3}{dt} = \alpha_{\text{glass}} \tau_{\text{cov}} G_{\text{tilt}} A + h_{\text{gap}} A (T_2 - T_3) - h_{\text{cond}} A (T_3 - T_4) \quad (\text{A3})$$

$$m_4 c_4 \frac{dT_4}{dt} = \alpha_{\text{pv}} \tau_{\text{cov}} \tau_{\text{mod}} G_{\text{tilt}} A - P_{\text{elec}} + h_{\text{cond}} A (T_3 - T_4) - h_{\text{cond}} A (T_4 - T_5) \quad (\text{A4})$$

$$m_5 c_5 \frac{dT_5}{dt} = h_{\text{cond}} A (T_4 - T_5) - h_{\text{cond}} A (T_5 - T_6) \quad (\text{A5})$$

$$m_6 c_6 \frac{dT_6}{dt} = h_{\text{cond}} A (T_5 - T_6) - h_{\text{fluid}} A_{\text{tube}} (T_6 - T_7) - h_{\text{cond}} A (T_6 - T_8) \quad (\text{A6})$$

$$m_7 c_7 \frac{dT_7}{dt} = h_{\text{fluid}} A_{\text{tube}} (T_6 - T_7) - \dot{m} c_{p,w} (T_7 - T_{\text{in}}) \quad (\text{A7})$$

$$m_8 c_8 \frac{dT_8}{dt} = h_{\text{cond}} A (T_6 - T_8) - h_{\text{ins}} A (T_8 - T_a) \quad (\text{A8})$$

Here G_{tilt} is the plane-of-array irradiance of Equation (3b); $h_{\text{conv}}(v)$ is the McAdams coefficient of Equation (9), with the sky radiative loss lumped as h_{rad} against the ambient T_a ; P_{elec} follows Equations (4) and (5); and the capacitances $m_i c_i$ are those of Table 3. Equations (A4) and (A7) are Equations (6) and (7) of the main text. The absorber–back coupling in (A6)/(A8) is reciprocal (energy-conserving), correcting the non-conservative one-way coupling of the original per-paper model noted in Appendix A.1.

References

1. International Energy Agency (IEA). *Renewables 2024: Analysis and Forecasts to 2030*; IEA: Paris, France, 2024. Available online: <https://www.iea.org/reports/renewables-2024> (accessed on 16 July 2026).
2. Dubey, S.; Sarvaiya, J.N.; Seshadri, B. Temperature Dependent Photovoltaic (PV) Efficiency and Its Effect on PV Production in the World—A Review. *Energy Procedia* **2013**, *33*, 311–321.
3. Herrando, M.; Ramos, A.; Zabalza, I.; Markides, C.N. A comprehensive assessment of alternative absorber-exchanger designs for hybrid PVT-water collectors. *Appl. Energy* **2019**, *235*, 1583–1602.
4. Kramer, K.S.; Mehnert, S.; Munz, G.; Helmling, S.; Lämmle, M. Photovoltaic Thermal Technology Collectors, Systems, and Applications. *Energy Technol.* **2023**, *11*, 2300378. <https://doi.org/10.1002/ente.202300378>.
5. Weiss, W.; Spörk-Dür, M. *Solar Heat Worldwide—Global Market Development and Trends in 2023*, 2024 ed.; IEA Solar Heating and Cooling Programme (SHC TCP): Gleisdorf, Austria, 2024. Available online: <https://www.iea-shc.org/solar-heat-worldwide> (accessed on 16 July 2026).
6. Forman, C.; Muritala, I.K.; Pardemann, R.; Meyer, B. Estimating the global waste heat potential. *Renew. Sustain. Energy Rev.* **2016**, *57*, 1568–1579.
7. Carrillo, A.J.; González-Aguilar, J.; Romero, M.; Coronado, J.M. Solar Energy on Demand: A Review on High Temperature Thermochemical Heat Storage Systems and Materials. *Chem. Rev.* **2019**, *119*, 4777–4816.
8. Li, T.X.; Wang, R.Z.; Kiplagat, J.K.; Kang, Y.T. Performance analysis of an integrated energy storage and energy upgrade thermochemical solid–gas sorption system for seasonal storage of solar thermal energy. *Energy* **2013**, *50*, 454–467.
9. Thinsurat, K.; Ma, Z.; Roskilly, A.P.; Bao, H. Compressor-assisted thermochemical sorption integrated with solar photovoltaic-thermal collector for seasonal solar thermal energy storage. *Energy Convers. Manag. X* **2022**, *15*, 100248.
10. Korkua, S.K.; Thubsuang, U.; Sakphrom, S.; Dash, S.K.; Tesanu, C.; Thinsurat, K. Simulation-Driven Optimization of Thermochemical Energy Storage in SrCl_2 -Based System for Integration with Solar Energy Technology. *Inventions* **2025**, *10*, 9. <https://doi.org/10.3390/inventions10010009>.
11. Ma, Z.; Bao, H.; Roskilly, A.P. Seasonal solar thermal energy storage using thermochemical sorption in domestic dwellings in the UK. *Energy* **2019**, *166*, 213–222.
12. Scapino, L.; Zondag, H.A.; Van Bael, J.; Diriken, J.; Rindt, C.C.M. Sorption heat storage for long-term low-temperature applications: A review on the advancements at material and prototype scale. *Appl. Energy* **2017**, *190*, 920–948.
13. Çetin, G.; Özkaraca, O.; Keçebaş, A. Development of PID based control strategy in maximum exergy efficiency of a geothermal power plant. *Renew. Sustain. Energy Rev.* **2021**, *137*, 110623.
14. Tiwari, A.; Sodha, M.S. Performance evaluation of solar PV/T system: An experimental validation. *Sol. Energy* **2006**, *80*, 751–759.

15. Keçebaş, A.; Yabanova, İ. Economic analysis of exergy efficiency based control strategy for geothermal district heating system. *Energy Convers. Manag.* **2013**, *73*, 1–9.
16. Badescu, V. Optimal control of flow in solar collectors for maximum exergy extraction. *Int. J. Heat Mass Transf.* **2007**, *50*, 4311–4322.
17. Araújo, A.; Pereira, V. Solar thermal modeling for rapid estimation of auxiliary energy requirements in domestic hot water production: Proportional flow rate control. *Energy* **2017**, *138*, 668–681.
18. Jiang, L.; Liu, W.; Lin, Y.C.; Wang, R.Q.; Zhang, X.J.; Hu, M.K. Hybrid thermochemical sorption seasonal storage for ultra-low temperature solar energy utilization. *Energy* **2022**, *239*, 122068.
19. Zou, W.; Yu, G.; Du, X. Electro-Thermal Transient Characteristics of Photovoltaic–Thermal (PV/T)–Heat Pump System. *Energies* **2025**, *18*, 4513.
20. Fudholi, A.; Zohri, M.; Jin, G.L.; Ibrahim, A.; Yen, C.H.; Othman, M.Y.; Ruslan, M.H.; Sopian, K. Energy and exergy analyses of photovoltaic thermal collector with V-groove. *Sol. Energy* **2018**, *159*, 742–750.
21. Kazemian, A.; Salari, A.; Hakkaki-Fard, A.; Ma, T. Numerical investigation and parametric analysis of a photovoltaic thermal system integrated with phase change material. *Appl. Energy* **2019**, *238*, 734–746.
22. Şirin, C.; Goggins, J.; Hajdukiewicz, M. A review on building-integrated photovoltaic/thermal systems for green buildings. *Appl. Therm. Eng.* **2023**, *229*, 120607.
23. Yabanova, İ.; Keçebaş, A. Development of ANN model for geothermal district heating system and a novel PID-based control strategy. *Appl. Therm. Eng.* **2013**, *51*, 908–916.
24. Lave, M.; Kleissl, J. Solar variability of four sites across the state of Colorado. *Renew. Energy* **2010**, *35*, 2867–2873.
25. Yan, H.; Chong, D.; Wang, Z.; Liu, M.; Zhao, Y.; Yan, J. Dynamic performance enhancement of solar-aided coal-fired power plant by control strategy optimization with solar/coal-to-power conversion characteristics. *Energy* **2022**, *244*, 122564.
26. Duffie, J.A.; Beckman, W.A.; Blair, N. *Solar Engineering of Thermal Processes, Photovoltaics and Wind*, 5th ed.; Wiley: Hoboken, NJ, USA, 2020.
27. Erbs, D.G.; Klein, S.A.; Duffie, J.A. Estimation of the diffuse radiation fraction for hourly, daily and monthly-average global radiation. *Sol. Energy* **1982**, *28*, 293–302.
28. Bejan, A. *Advanced Engineering Thermodynamics*, 4th ed.; Wiley: Hoboken, NJ, USA, 2016.
29. Petela, R. Exergy of undiluted thermal radiation. *Sol. Energy* **2003**, *74*, 469–488.
30. Lave, M.; Kleissl, J.; Stein, J.S. A Wavelet-Based Variability Model (WVM) for Solar PV Power Plants. *IEEE Trans. Sustain. Energy* **2013**, *4*, 501–509.

Disclaimer/Publisher’s Note: The statements, opinions and data contained in all publications are solely those of the individual author(s) and contributor(s) and not of MDPI and/or the editor(s). MDPI and/or the editor(s) disclaim responsibility for any injury to people or property resulting from any ideas, methods, instructions or products referred to in the content.