

Optimized Efficiency to Minimize Transmission Losses and Reduce Overall System Operating Costs of 1100 kV Converter Transformers for Power Transmission System

Mohammad Ali* 

School of Science & Engineering, Atlantic International University, Honolulu, HI 96813, USA.

***Corresponding Author:** Mohammad Ali, ORCID: <https://orcid.org/0009-0001-2184-7978>, School of Science & Engineering, Atlantic International University, Honolulu, HI 96813, USA.

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Abstract

Converter transformers are the largest single source of controllable loss in a line-commutated ultra-high-voltage direct-current (UHVDC) converter station, yet they are usually specified with loss-capitalization rules developed for sinusoidally loaded power transformers. This paper develops and applies a three-layer framework that minimizes the transmission losses and the overall operating cost of 1100 kV converter transformers. Layer 1 is an analytical electromagnetic-thermal-economic design model of a single-phase, two-wound-limb unit whose total owning cost (TOC) is minimized by differential evolution and cross-checked by particle swarm optimization over seven variables, including the strand thickness of continuously transposed conductors, the core-steel grade and magnetic tank shunts, subject to impedance, over-excitation, hot-spot, transport-height and 600 t transport-mass limits. Layer 2 is a harmonic-aware loss model that computes the valve-winding spectrum from the commutation overlap at every load and corrects the IEC h^2 eddy-loss law for skin effect. Layer 3 coordinates the firing-angle and on-load tap-changer set-point and stages the coolers for minimum loss over an 8760-h load and ambient profile. Applied to a ± 1100 kV, 12 GW bipole with 611.8 MVA units, the TOC-optimal design cuts rated losses from 1288 kW (conventional design) and 1725 kW (minimum-first-cost design) to 1077 kW, raises full-load efficiency to 99.79 % and lowers the TOC by 2.75 % and 9.54 %, respectively. At station level, the three layers together reduce annual converter-transformer energy losses from 152.0 GWh to 122.9 GWh (−19.2 %), saving about USD 1.75 million and 16.6 kt of CO₂ per year. The study also shows that rated-point loss factors underestimate the annual load-loss energy by 4.4–7.1 % and that tap tracking lowers the no-load energy by 11.7 %.

Keywords: Converter Transformer, Differential Evolution, Efficiency Optimization, Harmonic Losses, Loss Capitalization, Total Owning Cost, Transmission Losses, UHVDC, Winding Eddy Current Loss.

1. Introduction

ULTRA-HIGH-VOLTAGE direct-current (UHVDC) transmission based on line-commutated converters (LCC) has become the preferred technology for moving bulk power over thousands of kilometres [1,2]. The ± 1100 kV Changji–Guquan link, energized in 2019, transmits 12 GW over about 3293 km and can deliver 66 billion kWh per year to eastern China each pole consists of two series-connected twelve-pulse converters with a rated DC current of 5455 A [3,4]. Its converter transformers are among the most demanding power transformers ever built: units rated at not less than 607 MVA, weighing more than 800 t and exceeding 30 m in length have been reported and a 587.1 MVA unit for the same project was type tested with a short-circuit impedance of $(22 \pm 1) \%$ and a maximum transport mass of 600 t [5–7]. A converter station contains 24 operating converter transformers (plus spares), and their no-load and load losses are a major item in the station loss budget defined by IEC 61803 and IEEE Std 1158 [8,9]. Because a UHVDC link operates at a high utilization factor

for 40 years or more, every kilowatt of transformer loss is paid for many times over in purchased energy, in generation capacity and in cooling auxiliary power. Utilities therefore capitalize losses through the purchase price, following the total owning cost (TOC) approach of IEEE Std C57.120 and the efficiency philosophy summarized in CIGRE TB 971 while minimum energy-performance requirements for large power transformers are set by EN 50629 and the EU ecodesign regulation [10–13]. Converter transformers differ from conventional power transformers in four respects that directly affect loss optimization. First, their windings carry the non-sinusoidal valve current of a six-pulse bridge, so the winding eddy-current and structural stray losses are amplified by harmonic factors defined in IEC/IEEE 60076-57-129 and IEEE Std C57.110 [14,15]. Second, the valve winding is stressed by combined AC, DC and polarity-reversal voltages, which fixes large oil-barrier insulation distances and therefore the leakage geometry [16,17]. Third, the short-circuit impedance is not a free design outcome but a tightly

specified converter parameter that controls commutation overlap and fault current [18,19]. Fourth, the size of the units is limited by transport height and mass [6].

Transformer design optimization has a long history, comprehensively surveyed in and the physical loss mechanisms are well documented [20-25]. Recent work combines finite-element analysis, response-surface models and NSGA-III to minimize losses and material mass of power transformers reviews the computation of stray load losses under harmonic conditions quantifies the thermal effect of harmonics with three-dimensional models and shows that cooling management can raise transformer efficiency [26-30]. Three gaps remain for UHVDC converter transformers. 1) Design optimizations are formulated for sinusoidal loading or for a single rated harmonic spectrum, although the spectrum of a converter transformer changes with load because the commutation overlap changes. 2) Loss capitalization multiplies the rated load loss by a K^2 -based loss-load factor, which ignores the load dependence of the harmonic factors and the reduction of core flux produced by tap-changer tracking. 3) Design-stage measures and operation-stage measures (tap and firing-angle set-points, cooler staging) are treated separately, so their combined effect on station-level operating cost is not quantified.

This paper addresses these gaps. Its contributions are as follows.

- A closed analytical electromagnetic, thermal and economic model of a single-phase, two-wound-limb 1100 kV converter transformer, including the impedance band, over-excitation, hot-spot, transport-height and transport-mass constraints, and the strand thickness of continuously transposed conductors (CTC) as a design variable.
- A harmonic-aware loss model in which the valve-winding spectrum is computed from the commutation overlap at each load point and the IEC h^2 eddy-loss law is corrected for skin effect, verified against the closed-form harmonic expression.

- A three-layer optimization framework: TOC-optimal design by differential evolution (DE) with particle swarm optimization (PSO) cross-check and a Pareto analysis (Layer 1); harmonic-aware annual loss evaluation (Layer 2); and firing-angle/on-load tap-changer (OLTC) set-point coordination plus loss-optimal cooling control (LOCC) over an 8760-h profile (Layer 3).
- A quantified station-level assessment of energy losses, operating cost, CO₂ emissions, reactive power and insulation ageing for a ± 1100 kV, 12 GW links, together with a sensitivity analysis of the economic assumptions.

Section II describes the system architecture and the converter model. Section III develops the mathematical model of the converter transformer, and Section IV formulates the optimization framework. Section V presents and discusses the results, and Section VI concludes the paper.

2. System Architecture and Converter Model

2.1. System Architecture

Fig. 1 shows the architecture of the studied ± 1100 kV, 12 GW bipolar LCC-UHVDC link, modelled on the Changji-Guquan parameters [3-6]. Each pole comprises a high-voltage (HV) and a low-voltage (LV) twelve-pulse valve group in series; each valve group is fed by a bank of three Yy0 and three Yd11 single-phase converter transformers. The sending-end station therefore operates 24 converter transformers with four DC insulation levels (275, 550, 825 and 1100 kV) [6]; the six units that feed the uppermost bridges of the two poles carry the full 1100 kV DC stress. In this paper, “1100 kV converter transformers” denotes the converter transformers of the ± 1100 kV system; all 24 units have the same rating, and a Yy0 unit is studied as representative. The receiving end is connected to the 1000 kV and 500 kV AC grids through a layered arrangement [6]. The proposed loss-optimization framework acts on the converter transformers at two stages: at design (Layers 1 and 2) and in operation (Layer 3). The main data are listed in Table I.

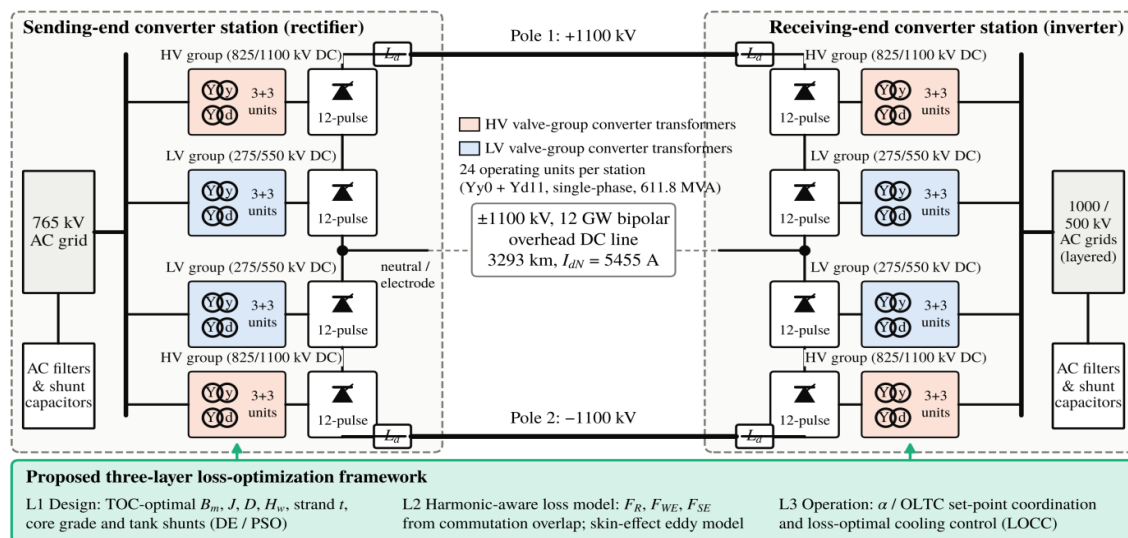


Figure 1: System Architecture of the ± 1100 kV, 12 GW Bipolar LCC-UHVDC Link With Series-Connected Twelve-Pulse Valve Groups, the Converter Transformers of the HV and LV Valve Groups, and the Proposed Three-Layer Loss-Optimization Framework

Quantity	Value
Rated DC power / voltage	12 GW / ± 1100 kV
Rated DC current I_{dN}	5455 A
Line length / annual energy	3293 km / 66 TWh
Valve groups per pole	2×12 -pulse (2×550 kV)
Operating units per station	24 (6 at 1100 kV DC level)
Line-side voltage U_L	765 kV (LL), 441.7 kV (winding)
Rated firing angle α_N	15°
Specified impedance u_k	22 % (band 21–23 %)
Ideal no-load DC voltage U_{d0N}	321.3 kV per bridge
Valve-side voltage U_{vN} (LL)	237.9 kV
Unit rating S_T , from (3)	611.8 MVA
Yy0 valve winding: U_V / I_{vN}	137.4 kV / 4454 A
Line winding current I_L	1385 A
Rated overlap angle μ_N	26.8°
Active power per unit P_u	500 MW
Cooling	ODAF, N_d+1 coolers

Data from [3-6]; derived quantities from (1)– (5).

Table 1: Main Data of the Studied UHVDC System and Converter Transformer

2.2. Twelve-Pulse Valve Group and Converter-Transformer Rating

The circuit diagram of one twelve-pulse valve group is given in Fig. 2. The two six-pulse bridges are connected in series on the DC side and fed by the Yy0 and Yd11 banks, whose 30° phase displacement cancels the harmonics of order

$h = 6k \pm 1$ with odd k on the line side [18,31]. Inside each transformer, however, the full six-pulse spectrum flows; this is the fundamental reason why converter-transformer losses must be evaluated with the six-pulse spectrum and not with the twelve-pulse line current. For one six-pulse bridge, the DC voltage is [18,19]

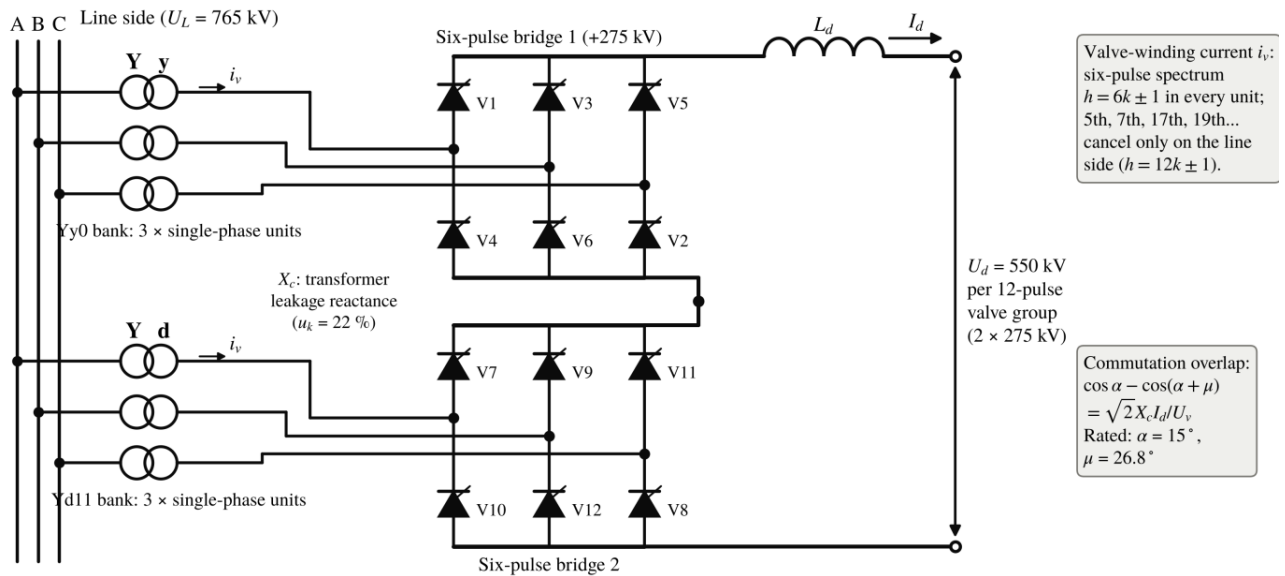


Figure 2: Circuit Diagram of a Twelve-Pulse Valve Group: Line-Side Bus, Yy0 and Yd11 Banks of Single-Phase Converter Transformers, Two Series-Connected Six-Pulse Thyristor Bridges, Smoothing Reactor L_d , and Commutation Reactance X_c Formed by the Transformer Leakage Reactance

$$U_d = U_{d0} \cos \alpha - \frac{3}{\pi} X_c I_d \quad (1)$$

$$U_{d0} = \frac{3\sqrt{2}}{\pi} U_v \approx 1.35 U_v \quad (2)$$

where U_{d0} is the ideal no-load DC voltage, U_v the line-to-line valve-side voltage, α the firing angle and X_c the commutation reactance, i.e., the leakage reactance of the converter

transformer referred to the valve side. The rating of one single-phase unit of a three-unit bank follows from the ideal rms valve current $I_{vN} = \sqrt{2/3} I_{dN}$:

$$S_T = \frac{\pi}{9} U_{d0N} I_{dN} = \frac{\sqrt{2}}{3} U_{vN} I_{dN} \quad (3)$$

The commutation of the current between two valves lasts for the overlap angle μ , which satisfies

$$\cos\alpha - \cos(\alpha + \mu) = \frac{\sqrt{2}X_c I_d}{U_v} \quad (4)$$

$$d_{xN} = \frac{3X_c I_{dN}}{\pi U_{d0N}} = \frac{u_k}{2} \quad (5)$$

where d_{xN} is the relative inductive voltage drop at rated current and u_k the per-unit short-circuit impedance on the base of (3); equivalently, the right-hand side of (4) equals $2d_{xN} KU_{d0N} / U_{d0}$ with $K = I_d / I_{dN}$. With $U_d = 275$ kV per bridge, $\alpha_N = 15^\circ$ and $u_k = 0.22$, (1)–(5) give $U_{d0N} = 321.3$ kV, $U_{vN} = 237.9$ kV and $S_T = 611.8$ MVA. This is consistent with the “at least 607 MVA” rating reported for the 1100 kV units of the project and $I_{vN} = 4454$ A coincides with the valve-side current of the tested 1100 kV unit [5,6]. The rated overlap angle is $\mu_N = 26.8^\circ$.

2.3. Valve-Winding Current and Harmonic Spectrum

With constant DC current, the valve current rises during commutation according to

$$i_v(\theta) = I_d \frac{\cos\alpha - \cos\theta}{\cos\alpha - \cos(\alpha + \mu)}, \quad \alpha \leq \theta \leq \alpha + \mu \quad (6)$$

where θ is measured from the natural commutation instant. The valve current remains at I_d until the next commutation 120° later, falls with the mirror-image law, and repeats with negative sign in the second half-cycle. The phase current of the Yy0 valve winding is therefore a trapezoid-like wave whose spectrum contains only the orders $h = 6k \pm 1$. The spectrum is obtained in this work by a fast Fourier transform (FFT) of the sampled waveform (8192 points per cycle) and verified against the classical closed form [18,19]

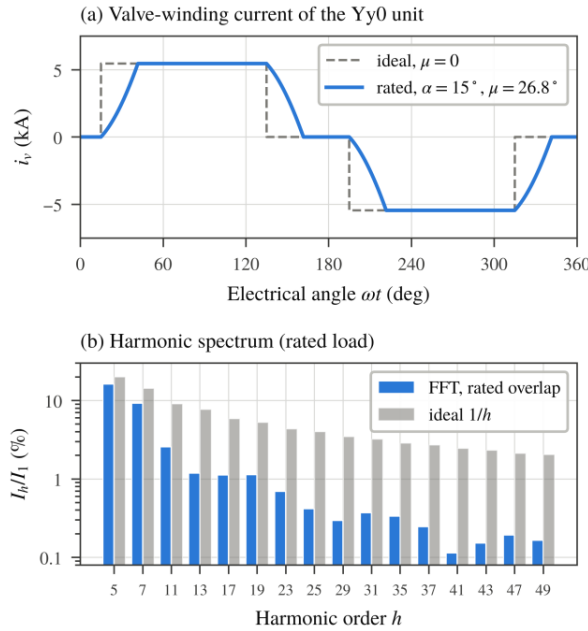


Figure 3: Valve-Winding Current of the Yy0 Converter Transformer at Rated Load ($\alpha = 15^\circ$, $\mu = 26.8^\circ$) Compared with the Ideal 120° Rectangular Wave. (b) Harmonic Spectrum from the FFT (Logarithmic Scale) Compared with the Ideal $1/h$ law

$$\frac{I_h}{I_{1,0}} = \frac{\sqrt{A_h^2 + B_h^2 - 2A_h B_h \cos(2\alpha + \mu)}}{h[\cos\alpha - \cos(\alpha + \mu)]} \quad (7)$$

with $A_h = \sin[(h-1)\mu/2] / (h-1)$, $B_h = \sin[(h+1)\mu/2] / (h+1)$ and $I_{1,0} = \sqrt{6} I_d / \pi$. Fig. 3 compares the rated waveform with the ideal rectangular wave and shows the spectrum, and Table II lists the harmonic ratios $k_h = I_h / I_{1N}$ with the per-

harmonic weights used later in the loss model. The overlap reduces the high-order harmonics far below the ideal $1/h$ law; nevertheless, the 5th and 7th harmonics still carry 16.2 % and 9.2 % of the fundamental at rated load. The rated rms current is $I_{rms} = 4292$ A, i.e., 3.6 % below the ideal I_{vN} , and the rated fundamental is $I_{1N} = 4217$ A.

h	k_h (%)	Err. (ppm)	$k_h^2 h^2$	$k_h^2 h^{0.8}$	χ_h
1	100.00	—	1.0000	1.0000	1.0000
5	16.19	0	0.6555	0.0950	0.9998
7	9.20	0	0.4150	0.0402	0.9995
11	2.56	2	0.0795	0.0045	0.9988
13	1.18	2	0.0237	0.0011	0.9983
17	1.12	4	0.0361	0.0012	0.9971
19	1.13	5	0.0461	0.0013	0.9963
23	0.69	8	0.0255	0.0006	0.9946
25	0.42	7	0.0109	0.0002	0.9936
29	0.30	11	0.0073	0.0001	0.9915
31	0.37	14	0.0132	0.0002	0.9903
35	0.34	19	0.0138	0.0002	0.9876
37	0.25	16	0.0082	0.0001	0.9862
41	0.11	19	0.0022	0.0000	0.9831
43	0.15	28	0.0043	0.0000	0.9814
47	0.19	33	0.0081	0.0001	0.9779
49	0.16	31	0.0065	0.0001	0.9760
Σ	—	—	2.356	1.145	—

$k_h = I_h/I_{1N}$ from FFT; |Err.|: relative deviation of (7) from the FFT; χ_h : skin-effect factor (24) for $t = 1.3$ mm. Last row: F_{WE} and F_{SE} (IEC laws).

Table 2: Rated Valve-Windind Harmonic Spectrum and Loss Weights

3. Mathematical Model of the Converter Transformer

3.1. Construction and Design Variables

The studied unit is a single-phase, core-type transformer with two wound limbs and two unwound return limbs, as shown in Fig. 4(a). The two wound limbs carry identical windings that are connected in parallel, so each limb handles half the rated current at full voltage; the flux of each wound limb returns half through the inner yoke and half through the adjacent return limb, which allows yokes and return limbs of half cross-section and keeps the core height within the transport profile [16,23]. From the core outwards, each limb carries the line winding (LW, with its regulating

winding RW lumped in), an oil-barrier main gap g that holds the combined AC, DC and polarity-reversal stresses, and the valve winding (VW). The leakage-field (magnetomotive-force) profile across this arrangement is sketched in Fig. 4(b). The design is described by the vector

$$\mathbf{x} = [B_m, J, D, H_w, t, s_g, s_{sh}]^T \quad (8)$$

where B_m is the peak core flux density, J the current density of both windings, D the core diameter, H_w the winding height, t the radial strand thickness of the CTC, $s_g \in \{0,1,2\}$ the core-steel grade and $s_{sh} \in \{0,1\}$ the use of magnetic tank shunts.

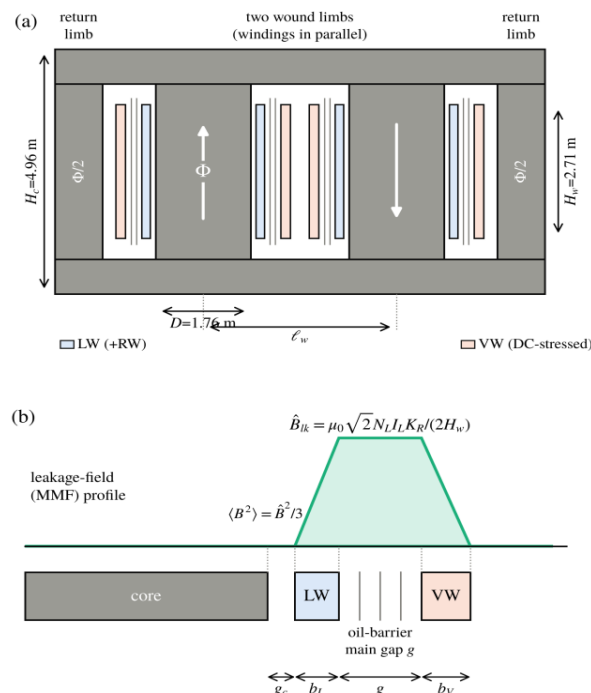


Figure 4: Schematic of the 1100 kV Converter Transformer (TOC-Optimal Design D3, to Scale): (a) Core with two Wound Limbs and two Return Limbs, Windings and Main Dimensions; (b) Radial Cross-Section of One Winding Pair with the Leakage-Field (MMF) Profile Used in (17)–(25)

3.2. Electromagnetic Design Equations

The net core cross-section and the voltage per turn are

$$A_c = k_{st} k_c \frac{\pi D^2}{4} \quad (9)$$

$$e_t = \sqrt{2} \pi f B_m A_c \quad (10)$$

where k_{st} is the stacking factor of the grade and $k_c = 0.94$ the circle-filling factor of the stepped core? With the windings of the two limbs in parallel, the turns and conductor cross-sections are

$$N_L = \frac{U_L}{e_t}, N_V = \frac{U_V}{e_t}, a_L = \frac{I_L}{2J}, a_V = \frac{I_{vN}}{2J} \quad (11)$$

and the radial builds follow from the winding space factors, which decrease for thin strands because the strand insulation δ_{ins} occupies a larger share of the conductor:

$$b_i = \frac{N_i a_i}{H_w k_{w,i}}, \quad k_{w,i} = k_{w0,i} \frac{t}{t + \delta_{ins}} \quad (12)$$

With the inner, mean and outer diameters of the windings and of the main gap obtained by stacking D , the core gap g_c , b_L , g and b_V , the copper volumes of the main windings ($V_{Cu} = V_L + V_V$) and the copper and core masses are

$$V_L = 2\pi N_L D_{Lm} a_L, \quad V_V = 2\pi N_V D_{Vm} a_V \quad (13)$$

$$m_{Cu} = \gamma_{Cu} [(1 + k_{RW}) V_L + V_V] \quad (14)$$

$$m_{Fe} = k_{cr} \gamma_{Fe} A_c (3L_w + \ell_w + 2\ell_r + w_r) \quad (15)$$

where $L_w = H_w + 2e_y$ is the window height with end insulation e_y , ℓ_w and ℓ_r are the limb pitches (wound-wound and wound-

return), $w_r = D/2$ is the width of the half-section return limb, k_{RW} accounts for the regulating winding and k_{cr} for the corners. The no-load loss uses a power-law fit of the specific loss of grain-oriented steel in the 1.45–1.80 T range [32,33]:

$$P_0 = k_{NL} k_{BF} m_{Fe} p_{17} \left(\frac{B_m}{1.7} \right)^\beta \quad (16)$$

in which p_{17} is the specific loss at 1.7 T and 50 Hz, k_{BF} the building factor and $k_{NL} = 1.05$ an allowance for the DC magnetization and the distorted valve-side voltage that are characteristic of converter transformers [14], [16]. The reactive part of the short-circuit impedance is computed with the classical ampere-turn-diagram method [23], [24]:

$$u_x = \frac{2\pi f \mu_0 (N_L I_L / 2) \Sigma_{ATD} K_R}{H_w e_t} \quad (17)$$

$$\Sigma_{ATD} = \pi \left(\frac{D_{Lm} b_L}{3} + D_{gm} g + \frac{D_{Vm} b_V}{3} \right) \quad (18)$$

where $K_R = 1 - \lambda(1 - e^{-\pi H_w / \lambda}) / (\pi H_w)$ is the Rogowski factor with $\lambda = b_L + g + b_V$. The impedance is $(u_x^2 + u_r^2)^{1/2}$ with $u_r = P_R / S_T$.

3.3. Harmonic-Aware Loss Model

Fig. 5 shows the harmonic-domain equivalent circuit used to separate the loss components. On the valve side the converter acts as a harmonic current source I_h , and the series resistances $R_1(h)$ and $R_2'(h)$ increase with frequency because of the winding eddy and structural stray losses [14,15]. The DC-resistance loss at the nominal rms current I_{vN} (and the corresponding line current), including leads and the in-circuit share k_{RW}' of the regulating winding, is

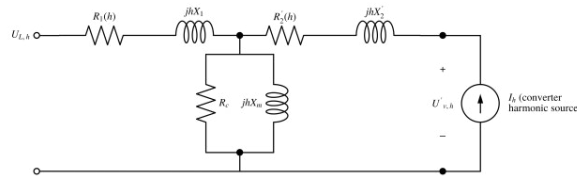


Figure 5: Harmonic-Domain T-Equivalent Circuit of the Converter Transformer Referred to the Line Winding. The Converter is a Harmonic Current Source I_h ; $R_1(h)$ and $R_2'(h)$ Include the Frequency-Dependent Eddy and Stray Losses, and $R_c || j\omega X_m$ Represents the Core

$$P_R = k_{ld} \rho_{75} J^2 (V_{Cu} + k_{RW}' V_L) \quad (19)$$

with ρ_{75} the copper resistivity at 75 °C and V_{Cu} the copper volume of the main windings. The axial leakage field rises linearly across each winding to the peak value

$$\hat{B}_{lk} = \frac{\sqrt{2} \mu_0 N_L (I_{L1}/2) K_R}{H_w} \quad (20)$$

where $I_{L1} = (I_{1N}/I_{vN}) I_L$ is the rated fundamental line current, so the mean square of the field over the winding volume is $\hat{B}_{lk}^2/3$. For a strand of thickness t in a tangential field, the exact eddy-current loss per unit volume is [34,35]

$$p_{WE1} = k_{end} \frac{\hat{B}_{lk}^2}{3\mu_0^2} \cdot \frac{F(\xi)}{\sigma \delta t} \quad (21)$$

$$F(\xi) = \frac{\sinh \xi - \sin \xi}{\cosh \xi + \cos \xi}, \quad \xi = \frac{t}{\delta}, \quad \delta = \sqrt{\frac{2\rho_{75}}{\omega \mu_0}} \quad (22)$$

where $k_{end} = 1.3$ accounts for the radial field at the winding ends and $\delta = 10.30$ mm is the skin depth at 50 Hz and 75 °C. For $\xi \ll 1$, (21) reduces to the familiar low-frequency result

$$p_{WE1} \approx k_{end} \frac{\sigma \omega^2 \hat{B}_{lk}^2 t^2}{72} \quad (23)$$

which is the basis of the h^2 law of [14], [15]. At harmonic order h the skin depth decreases as $\delta/\sqrt{h}$; the ratio of the

exact harmonic loss to the h^2 extrapolation is therefore

$$\chi_h = \frac{\sqrt{h} F(\xi \sqrt{h})}{h^2 F(\xi)} \quad (24)$$

The structural stray loss in clamps and tank is modelled as proportional to the square of the leakage field and to the area it illuminates, with a reduction η_{sh} when magnetic tank shunts are fitted [23,27]:

$$P_{SE1} = 2c_{SE} \hat{B}_{lk}^2 D_{Vo} H_w (1 - \eta_{sh} s_{sh}) \quad (25)$$

The load loss at an operating point with load factor $K = I_d / I_{dN}$ and firing angle α is then

$$P_{LL}(K) = P_R F_R + P_{WE1} F_{WE} + P_{SE1} F_{SE} \quad (26)$$

$$F_R = \left(\frac{I_{rms}}{I_{vN}} \right)^2 \quad (27)$$

$$F_{WE} = \sum_h k_h^2 h^2 \chi_h, \quad F_{SE} = \sum_h k_h^2 h^{0.8} \quad (28)$$

where $k_h = I_h / I_{1N}$ is computed from (6) at the actual (K, α, μ) , $P_{WE1} = p_{WE1} V_{cu}$, and the stray-loss exponent 0.8 follows [14]. F_R converts P_R to the actual rms current, which at rated load is 3.6 % below I_{vN} because of the overlap. At the rated point, $F_R = 0.9287$, $F_{SE} = 1.145$ and $F_{WE} = 2.356$ by the IEC h^2 law (2.354 with χ_h for $t = 1.3$ mm; Table II). The converter operating point is evaluated with the specified $u_k = 0.22$ for all designs; a deviation within the ± 1 % band changes μ_N by less than 0.8° and F_{WE} by about 2.6 %.

3.4. Thermal Model and Constraints

Directed-flow ODAF cooling is assumed. The average winding-to-oil gradient is related to the surface heat flux of the disc winding [36-38]:

$$q = \frac{p_{cu} k_w h_d}{2\varphi}, \quad g_w = g_{w0} \left(\frac{q}{q_0} \right)^{0.8} \quad (29)$$

where g_w is the winding-to-oil gradient, p_{cu} the winding loss per unit copper volume at rated harmonic load, k_w the larger of the two winding space factors, h_d the disc pitch and φ the wetted fraction of the disc surface. The number of duty coolers and the top-oil rise are

$$N_d = \text{ceil} \left(\frac{P_0 + P_{LL}}{Q_c} \right), \quad \Delta\theta_{or} = \Delta\theta_{ref} \frac{P_0 + P_{LL}}{N_d Q_c} \quad (30)$$

with one additional standby cooler ($N_c = N_d + 1$), and the hot-spot rise must satisfy

$$\Delta\theta_{hs} = \Delta\theta_{or} + H g_w \leq 75 \text{ K} \quad (31)$$

with hot-spot factor $H = 1.3$ [36]. The remaining constraints are the impedance band, an over-excitation margin at 110 % voltage, the transport height of the core $H_c = L_w + 0.8D$ and the shipping mass, estimated in this study as $m_{ship} = 1.43(m_{Fe} + m_{Cu}) + 25$ t to cover insulation, clamping, the tank without

oil and fittings, which must not exceed the 600 t transport limit reported in [6]:

$$0.21 \leq u_k \leq 0.23, \quad 1.1B_m \leq 1.9 \text{ T} \quad (32)$$

$$H_c \leq 5.2 \text{ m}, \quad m_{ship} \leq 600 \text{ t} \quad (33)$$

3.5. Economic Model and Total Owning Cost

The purchase price is built up from the active materials, the other material and fixed costs, and the cooling and shunt options:

$$C_P = k_m (c_{Fe} m_{Fe} + c_{Cu} m_{Cu}) + C_{oth} \quad (34)$$

$$c_{Cu}(t) = c_{Cu0} \left[1 + \kappa \left(\frac{t_{ref}}{t} - 1 \right) \right] \quad (35)$$

where $C_{oth} = 0.6 c_{aux} (m_{Fe} + m_{Cu}) + C_{fix} + c_{cool} N_c + C_{sh} s_{sh}$ and (35) represents the price premium of CTC with thin strands ($t_{ref} = 2$ mm). Instead of a single rated operating point, the annual energy losses are accumulated over the bins b of the 8760- h load profile, each with duration τ_b , mean load K_b and the valve voltage U_{vb} set by the OLTC:

$$E_{NL} = P_0 \sum_b \tau_b \left(\frac{U_{vb}}{U_{vN}} \right)^{\beta}, \quad E_{LL} = \sum_b \tau_b P_{LL}(K_b) \quad (36)$$

The auxiliary energy is $E_{aux} = N_d P_{ac} T_{on}$ for conventional operation with all duty coolers running. With the present-value factor

$$PVF = \frac{1 - (1 + r)^{-n}}{r} \quad (37)$$

the total owning cost is

$$TOC = C_P + PVF C_e E_{\Sigma} + C_d P_{\Sigma} \quad (38)$$

where $E_{\Sigma} = E_{NL} + E_{LL} + E_{aux}$ is the annual loss energy, $P_{\Sigma} = P_0 + P_{LL} + N_d P_{ac}$ the rated loss demand, C_e the value of energy and C_d the capitalized capacity cost. Equation (38) is equivalent to the IEEE C57.120 form $C_P + A_{NL} P_0 + B_{LL} P_{LL} + C_{aux}$ [10], where $C_{aux} = PVF C_e E_{aux} + C_d N_d P_{ac}$ is the capitalized auxiliary power of the cooling plant, with the profile-based factors

$$A_{NL} = PVF C_e \frac{E_{NL}}{P_0} + C_d \quad (39)$$

$$B_{LL} = PVF C_e \frac{E_{LL}}{P_{LL}} + C_d \quad (40)$$

For the base case of Table III ($C_e = 0.06$ USD/kWh, $r = 6$ %, $n = 40$ years, $PVF = 15.05$), (39)–(40) give $A_{NL} = 7643$ USD/kW and $B_{LL} = 4288$ USD/kW for the optimal design. The conventional rated-point factors would be $A_{NL} = 8550$ USD/kW (constant no-load loss over 8585 energized hours) and $B_{LL} = 4140$ USD/kW (loss-load factor 0.422); the differences are discussed in Section V-B. All model constants are listed in Table III; the material and cost data are representative values adopted for this study and should be replaced by tender data in an actual evaluation.

Parameter	Value
Core grades $s_g = 0/1/2$	0.30 HGO / 0.27 HGO-DR / 0.23 HGO-LS
p_{17} (W/kg); k_{BF}	1.00/0.85/0.75; 1.10/1.12/1.15
k_{se} ; c_{Fe} (USD/kg)	0.970/0.965/0.960; 2.90/3.25/3.70
β ; k_{NL} ; k_c ; k_{cr}	3.0; 1.05; 0.94; 1.04
g_c ; g ; e_y (m)	0.10; 0.30; 0.42
Clearances inter-limb / return (m)	0.35 / 0.25
k_{w0L} ; k_{w0V} ; δ_{ins} (mm)	0.40; 0.36; 0.12
k_{RW} ; k_{RW}' ; k_{ld}	0.25; 0.10; 1.03
k_{end} ; c_{SE} (W/(T ² ·m ²)); η_{sh}	1.30; 1.0×10^5 ; 0.45
h_d (mm); φ ; g_{w0} at q_0	18.5; 0.70; 16 K at 1000 W/m ²
Q_c (kW); P_{ac} (kW); $\Delta\theta_{ref}$	400; 11; 50 K
C_e ; C_d	0.06 USD/kWh; 800 USD/kW
r ; n ; T_{on}	6 %; 40 years; 8585 h
k_m ; c_{Cu0} ; κ	1.45; 14 USD/kg; 0.5
c_{aux} ; C_{fix}	3.0 USD/kg; 8.2 MUSD
C_{cool} ; C_{sh}	85 kUSD per cooler; 0.15 MUSD
Profile: equivalent full-load hours	5500 h (mean $K = 0.641$ when on)
Ambient temperature (min / mean / max)	-22.7 / 7.0 / 37.1 °C

HGO: high-permeability grain-oriented steel; DR: domain-refined; LS: laser-scribed.

Table 3: Model Constants, Material and Economic Parameters

4. Optimization Framework

4.1. Problem Formulation (Layer 1)

The design problem minimizes the TOC subject to the constraints (31)– (33) and the bounds of Table IV. The constraints are handled with a static penalty:

$$\min_{\mathbf{x}} \Phi(\mathbf{x}) = \text{TOC}(\mathbf{x}) + w \sum_i \max[0, \hat{c}_i(\mathbf{x})] \quad (41)$$

where $\hat{c}_i \leq 0$ are the normalized constraints of (31)–(33) and $w = 10^7$ USD per unit violation, plus a fixed 2 MUSD offset for infeasible points, so that every feasible design dominates

every infeasible one. The discrete variables s_g and s_{sh} are handled by rounding. Three reference designs are compared: D1, the minimum-first-cost design obtained by minimizing C_p alone under the same constraints; D2, a conventional-practice design with $B_m = 1.70$ T, $J = 2.9$ A/mm², $t = 2.0$ mm, domain-refined steel and tank shunts, whose geometry (D , H_w) is optimized for TOC; and D3, the proposed TOC-optimal design. D1–D3 were obtained with 175 candidates, up to 500 generations and five independent seeds each; the algorithm comparison of Table VI uses an equal, smaller budget for DE and PSO.

Symbol	Description	Range / limit
B_m	Peak flux density	1.45–1.75 T
J	Current density	1.8–3.5 A/mm ²
D	Core diameter	1.30–1.95 m
H_w	Winding height	2.0–3.4 m
t	CTC strand thickness	1.0–2.8 mm
s_g	Core grade	{0, 1, 2}
s_{sh}	Tank shunts	{0, 1}
c_1, c_2	Impedance band	$0.21 \leq u_k \leq 0.23$
c_3	Over-excitation	$1.1B_m \leq 1.9$ T
c_4	Hot-spot rise	$\Delta\theta_{hs} \leq 75$ K
c_5	Core height	$H_c \leq 5.2$ m
c_6	Shipping mass	$m_{ship} \leq 600$ t

Table 4: Design Variables, Bounds and Constraints

4.2. Solution Algorithms

The problem is solved with DE in the rand/1/bin variant [39]. For each target vector $\mathbf{x}_p$, a mutant is formed from three distinct population members,

$$\mathbf{v}_i = \mathbf{x}_{r1} + F_m(\mathbf{x}_{r2} - \mathbf{x}_{r3}) \quad (42)$$

followed by binomial crossover ($u_{ij} = v_{ij}$ if a uniform random number is below CR or $j = j_{\text{rand}}$, otherwise $u_{ij} = x_{ij}$) and greedy selection. A Latin-hypercube initial population and dithered mutation factor $F_m \in [0.5, 1]$ with $CR = 0.7$ are used. As an independent check, a PSO with the constriction coefficient of Clerc and Kennedy [40,41] is run with the same evaluation

budget:

$$\mathbf{v}_i \leftarrow \zeta [\mathbf{v}_i + c_1 \mathbf{r}_1 \circ (\mathbf{p}_i - \mathbf{x}_i) + c_2 \mathbf{r}_2 \circ (\mathbf{p}_g - \mathbf{x}_i)] \quad (43)$$

with constriction coefficient $\zeta = 0.7298$, personal and global bests $\mathbf{p}_i$ and $\mathbf{p}_g$, $c_1 = c_2 = 1.4962$, velocity clamping at 20 % of the range and $\mathbf{x}_i \leftarrow \mathbf{x}_i + \mathbf{v}_i$. Both algorithms use 105 candidates per iteration and 190 iterations (19 950 evaluations). The Pareto front between purchase price and present value of losses is obtained with the ε -constraint method, minimizing C_p for a sequence of upper limits on the present value of losses.

4.3. Operational Optimization (Layer 3)

In operation, the rectifier controls the DC current while the OLTC keeps the firing angle near its set-point α_{ref} at rated DC voltage. From (1) and (5), the required ideal no-load voltage and the resulting core flux are

$$U_{d0}(K) = \frac{U_{d,br} + d_{xN} U_{d0N} K}{\cos \alpha_{\text{ref}}} \quad (44)$$

$$B_m(K) = B_{mN} \frac{U_{d0}(K)}{U_{d0N}} \quad (45)$$

because the OLTC changes the line-winding turns while the valve-winding turns, and hence the volts per turn at a given valve voltage, stay fixed. A lower α_{ref} therefore reduces the flux, the no-load loss and the reactive power, and the larger overlap reduces the harmonic content. The winding losses are corrected for the winding temperature θ_w [42]:

$$P_R(\theta_w) = P_{R,75} \frac{235 + \theta_w}{310} \quad (46)$$

$$P_{WE}(\theta_w) = P_{WE,75} \frac{310}{235 + \theta_w} \quad (47)$$

For loss-optimal cooling control, the number n of coolers in service is chosen every hour. The top-oil rise follows a first-order response with time constant $\tau_o = 90$ min towards its steady state, and the hot-spot temperature includes the effect of reduced directed oil flow on the gradient [36,37]:

$$\tau_o \frac{d\Delta\theta_o}{dt} = \Delta\theta_{\text{ref}} \frac{P_{\text{tot}}}{nQ_c} - \Delta\theta_o \quad (48)$$

$$\theta_{hs} = \theta_a + \Delta\theta_o + H g_{w,r} \frac{P_w}{P_{w,r}} \sqrt{\frac{N_d}{n}} \quad (49)$$

where P_w is the winding loss and the subscript r denotes rated values. In operation the gradient varies linearly with the winding loss, which corresponds to the exponent $y = 2$ recommended for directed-oil cooling in [36], whereas the design correlation (29) relates the gradients of different designs to their heat flux; the factor $\sqrt{N_d/n}$ represents the reduced directed oil flow when pumps are stopped. The gradient follows its steady state with a time constant of 7 min, and the mean winding temperature in (46)–(47) is $\theta_w = \theta_a + 0.8\Delta\theta_o + g_w$. The LOCC rule selects

$$n_{\text{opt}} = \arg \min_{1 \leq n \leq N_d} [P_{LL}(n) + nP_{ac}] \quad (50)$$

subject to $\theta_{hs} \leq 98$ °C and a top-oil limit of 85 °C. The rule trades the auxiliary power of pumps and fans against the temperature-dependent copper loss (46)–(47). The consequence for insulation life is monitored with the relative ageing rate of thermally upgraded paper [36]:

$$V_{\text{age}} = \exp\left(\frac{15000}{383} - \frac{15000}{\theta_{hs} + 273}\right), \quad L_{\text{age}} = \int V_{\text{age}} dt \quad (51)$$

4.4. Performance Indices and Implementation

The efficiency at load factor K is evaluated with the active power $P_u = 500$ MW of one unit at rated load and the load- and tap-dependent losses:

$$\eta(K) = \frac{KP_u}{KP_u + P_0(K) + P_{LL}(K)} \quad (52)$$

and the station-level energy saving and emission reduction of a strategy j relative to the reference $S0$ are

$$\Delta E_{st} = N_u (E_{S0} - E_{Sj}), \quad \Delta m_{CO_2} = \epsilon_g \Delta E_{st} \quad (53)$$

with $N_u = 24$ units and an assumed grid emission factor $\epsilon_g = 0.57$ t/MWh. The complete procedure is summarized in Fig. 6. The model, the optimizers and the hourly simulation are implemented in vectorized Python (NumPy/SciPy); one TOC evaluation takes a few microseconds per design, so a complete DE run requires about 0.28 s on a standard workstation. The synthetic 8760-h profile combines seasonal, diurnal (solar-export) and autoregressive components scaled to 5500 equivalent full-load hours (66 TWh/yr) with 175 h of outage; the ambient temperature represents a continental climate with a mean of 7.0 °C.

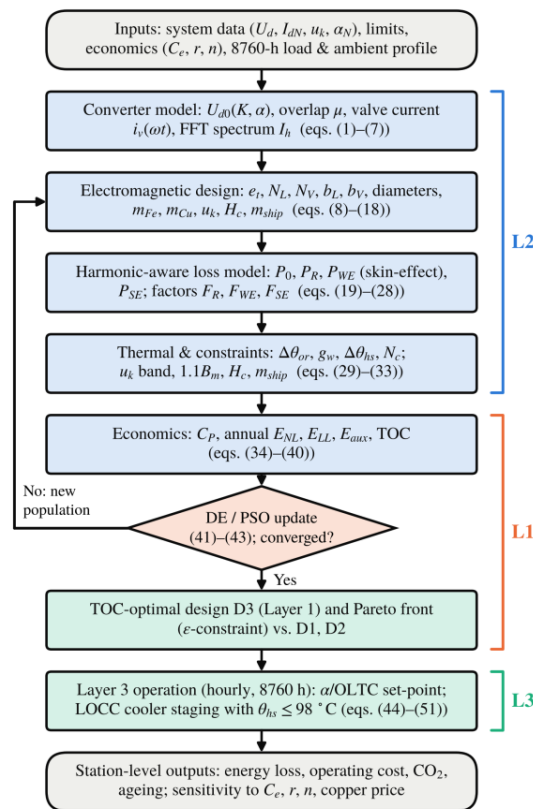


Figure 6: Flowchart of the Proposed Three-Layer Optimization Framework (L1: design, L2: Harmonic-Aware Loss Evaluation, L3: Operation). Equation Numbers Refer to This Paper

5. Results and Discussion

5.1. Model Verification

Four checks were made before the optimization. 1) The FFT spectrum agrees with the closed form (7) within 0.003 % for all orders up to 49 (Table II). 2) For $\mu \rightarrow 0$ the computed ratios reproduce the ideal law $I_h / I_1 = 1/h$ within 0.36 %, the residual being the sampling resolution. 3) The skin-effect factor (24) tends to unity for thin strands ($\chi_{49} = 0.99999$ at $t = 0.1$ mm), so the exact model reduces to the IEC law where it should. 4) The unit rating from (3) is 611.8 MVA, and the derived valve current, 4454 A, matches the published data of the 1100 kV units [5,6]. The model is analytical and has been calibrated to representative design practice; a detailed finite-element verification of the stray-field terms is part of future work (Section V-G).

5.2. Harmonic Loss Factors Over the Load Range

Fig. 7 shows that the harmonic factors depend strongly on load. As K decreases, the overlap angle falls from 26.8° to a few degrees and the current approaches a rectangular wave.

To compare loads, the factors are referred to the fundamental at the actual load, $\tilde{F}_{WE} = F_{WE}/k_1^2$, $\tilde{F}_{SE} = F_{SE}/k_1^2$ and $\tilde{F}_R = (I_{rms}/I_1)^2$ with $k_1 = I_1/I_{1N}$: $\tilde{F}_{WE}$ rises from 2.356 at rated load to 3.35 at $K = 0.55$ and 14.9 at $K = 0.05$, and $\tilde{F}_{SE}$ and $\tilde{F}_R$ show the same trend with smaller magnitudes. Weighted by the annual eddy-loss energy, the effective factor is 2.95, i.e., 25 % above the rated value. Consequently, the harmonic-aware annual load-loss energy (36) exceeds the conventional estimate (rated P_{LL} times $\sum K^2$) by 7.1 %, 6.0 % and 4.4 % for D1, D2 and D3, respectively; the smallest deviation belongs to D3 because thin strands reduce the eddy share of the load loss. Conversely, OLTC tracking lowers the flux at part load, so the no-load energy of D3 is 11.7 % below the value obtained with a constant P_0 . Both effects are captured by the profile-based factors A_{NL} and B_{LL} of (39)–(40). For strands of 1.3 mm the skin-effect correction changes F_{WE} by less than 1 %, so the IEC h^2 law is adequate in the optimal region, although it overestimates the harmonic eddy-loss factor of 2.8 mm strands by about 1.5 %.

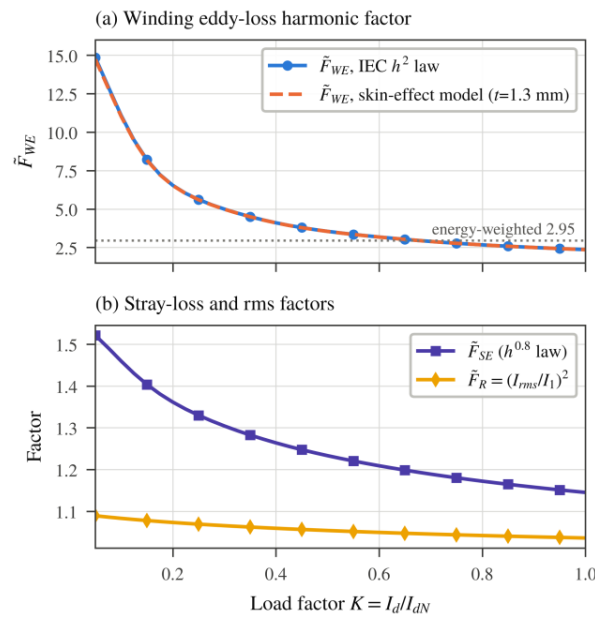


Figure 7: Harmonic Loss Factors Referred to the Fundamental at the Actual Load Versus Load Factor ($\alpha = 15^\circ$): (a) Winding Eddy-Loss Factor $\tilde{F}_{WE}$ by the IEC h^2 Law and by the Skin-Effect Model (24), with the Annual Energy-Weighted Value; (b) Structural Stray-Loss Factor $\tilde{F}_{SE}$ and Rms Factor $\tilde{F}_R$.

5.3. Optimal Designs

Table V compares the three designs. The minimum-first-cost design D1 drives B_m to the over-excitation limit (1.727 T) and J to its upper bound (3.50 A/mm²), uses the cheapest steel without shunts and thick strands ($t = 2.70$ mm); its hot-spot rise reaches the 75 K limit. The TOC-optimal design D3 lowers the flux density to 1.607 T and the current density to 2.44 A/mm², uses 1.3 mm strands, domain-refined steel and tank shunts, and increases the core diameter to 1.761

m. Its shipping mass of 594 t remains within the 600 t limit and its impedance of 22.22 % lies inside the specified band. Compared with D2, D3 increases the purchase price by USD 0.60 million but reduces the present value of losses by USD 1.12 million, giving a TOC saving of USD 0.52 million per unit (2.75 %) and USD 12.4 million for the 24 units of a station. Relative to D1 the saving is USD 1.92 million per unit (9.54 %).

Quantity	D1	D2	D3
B_m (T)	1.727	1.700	1.607
J (A/mm ²)	3.50	2.90	2.44
D (m)	1.544	1.691	1.761
H_w (m)	3.124	2.756	2.713
t (mm)	2.70	2.00	1.30
Core grade / shunts	0.30 HGO / no	0.27 DR / yes	0.27 DR / yes
e_t (V/tum)	655.3	769.2	788.8
N_L / N_V	674 / 210	574 / 179	560 / 174
m_{Fe} (t)	270.6	314.7	347.0
m_{Cu} (t)	37.3	41.6	50.7
m_{ship} (t)	465	535	594
H_c (m)	5.20	4.95	4.96
u_k (%)	23.00	21.00	22.22
P_0 (kW)	327.9	314.6	293.2
$P_R F_R$ (kW)	974.0	744.8	642.7
$P_{WE1} F_{WE}$ (kW)	309.5	173.8	86.5
$P_{SE1} F_{SE}$ (kW)	113.8	54.3	54.8
P_{LL} rated (kW)	1397.3	972.9	783.9
$P_0 + P_{LL}$ (kW)	1725.2	1287.5	1077.1
η at rated load (%)	99.656	99.743	99.785
Gradient g_w (K)	24.5	17.0	11.8
$\Delta\theta_{hs}$ (K)	75.0	62.4	60.2
Coolers $N_d + 1$	5 + 1	4 + 1	3 + 1

C_p (MUSD)	11.062	11.744	12.348
PV of losses (MUSD)	9.092	7.005	5.884
TOC (MUSD)	20.154	18.748	18.232

Losses at rated harmonic load, $\alpha = 15^\circ$, 75°C . PV: present value.

Table 5: Comparison of the Minimum-First-Cost (D1), Conventional (D2) and Toc-Optimal (D3) Designs

The convergence of both optimizers is shown in Fig. 8 and summarized in Table VI. All ten DE runs and all ten PSO runs reached the same optimum (spread below 1 USD), which indicates that the penalized landscape is well conditioned in

the seven variables. PSO came within 0.1 % of the optimum after 5.5 iterations on average and DE after 13.4 iterations, so both are fast enough for interactive tender evaluation.

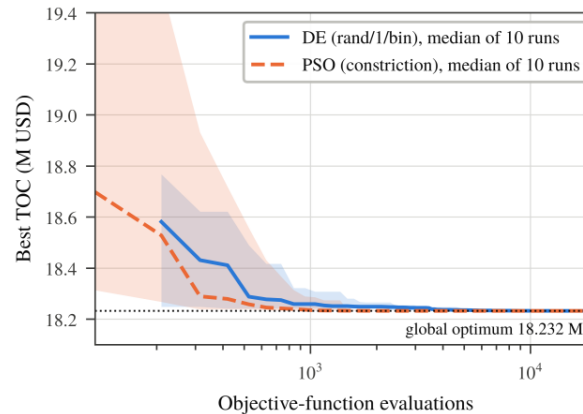


Figure 8: Convergence of DE and PSO for the TOC Minimization (Median and Min–Max Band of 10 Independent Runs Each, 105 Candidates Per Iteration)

Index	DE	PSO
Best TOC (MUSD)	18.2321	18.2321
Mean TOC (MUSD)	18.2321	18.2321
Std. deviation (USD)	0.19	< 0.001
Runs reaching optimum	10/10	10/10
Iterations to 0.1 % (mean)	13.4	5.5
Evaluations per run	19 950	19 950
Mean CPU time per run (s)	0.28	0.09

Table 6: Performance of the Optimization Algorithms (10 Runs Each)

Figure 9: Maps The Minimum TOC Achievable for Each Pair (B_m, J) when D and H_w are Re-Optimized and the Remaining Variables are Fixed at Their D3 Values. The Optimum Lies in a Broad Valley Around 1.60 T and 2.4 A/mm²; the Lower-Left Region is Infeasible Because the Transport-Mass Limit is Exceeded, and the Strip at the Right Is Excluded by the Over-Excitation Margin. The Flatness of the Valley (no More Than 0.12 % TOC Change Within ± 0.05 T and ± 0.2 A/mm²) Gives the Designer Freedom to Satisfy Secondary Requirements Such as Sound Level Without Economic Penalty, Whereas D2 and Especially D1 lie on the Steep Part of the Surface

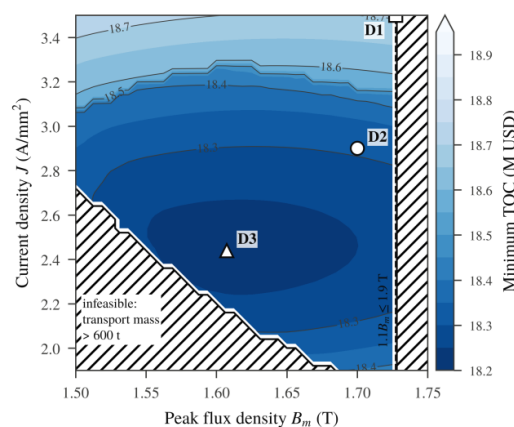


Figure 9: Minimum TOC Over Peak Flux Density and Current Density (D, H_w re-Optimized at Each Point; t, Grade and Shunts as in D3). Hatched Areas are Infeasible

The role of the CTC strand thickness is isolated in Fig. 10, where all other variables are re-optimized for each fixed t . Thinner strands reduce the winding eddy loss, which scales approximately with t^2 , but they lower the space factor and raise the conductor price. The TOC is minimal at $t = 1.3$ mm;

increasing the thickness to the conventional 2.0 mm costs USD 0.25 million per unit, and to 2.8 mm USD 1.14 million per unit. The steps near 2.2–2.5 mm are caused by switching of the discrete variables (core grade, shunts and cooler count) in the re-optimization.

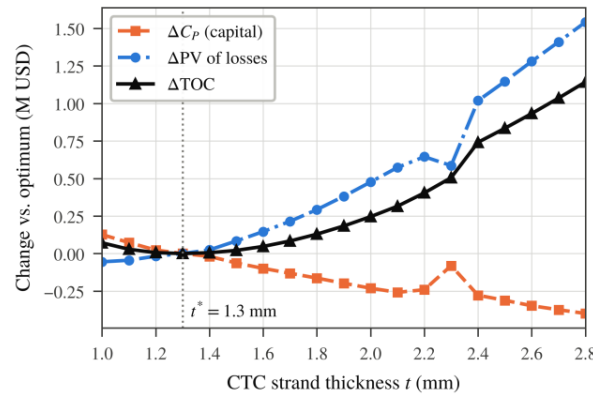


Figure 10: Effect of the CTC Strand Thickness on Capital Cost, Present Value of Losses and TOC, Each Relative to the Optimum (all Other Variables Re-Optimized)

The Pareto front of Fig. 11 shows the full trade-off between purchase price and present value of losses. D1 and D3 are both Pareto-optimal, D1 at the low-capital end and D3 at the point where the front touches the lowest iso-TOC line, whereas D2 lies slightly above the front, which shows

that the conventional choice of B_m , J and t is economically dominated. Beyond D3 the front flattens: an additional USD 0.47 million of capital buys only USD 0.41 million of present-value loss reduction.

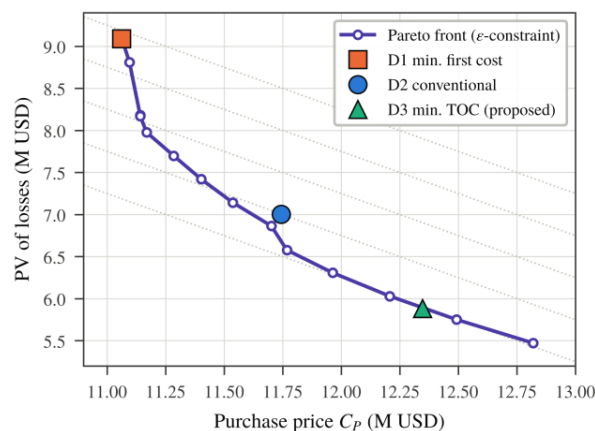


Figure 11: Pareto Front Between Purchase Price and Present Value of Losses (ϵ -Constraint Method) with Designs D1–D3. Dotted Lines are iso-TOC Lines Spaced by 0.5 MUSD

The loss breakdown at rated harmonic load (Fig. 12) shows where the savings come from. Relative to D2, D3 reduces the no-load loss by 315 → 293 kW and the load loss by 973 → 784 kW; the largest relative gain is in the winding eddy loss (174 → 87 kW), which is a direct consequence of the thin strands. In total the rated losses fall by 16.3 % relative to D2 and by 37.6 % relative to D1. The efficiency curves of Fig. 13

confirm the improvement over the whole load range: the full-load efficiency rises from 99.66 % (D1) and 99.74 % (D2) to 99.79 % (D3), and the maximum efficiency of D3, 99.82 %, occurs at $K = 0.53$, somewhat below the mean operating load of 0.64; between $K = 0.4$ and 1.0 the efficiency of D3 stays above 99.78 %.

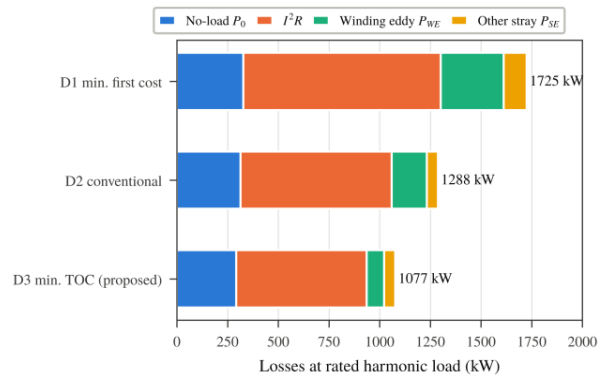


Figure 12: Breakdown of the Losses of Designs D1-D3 at Rated Harmonic Load ($\alpha = 15^\circ$, 75°C)

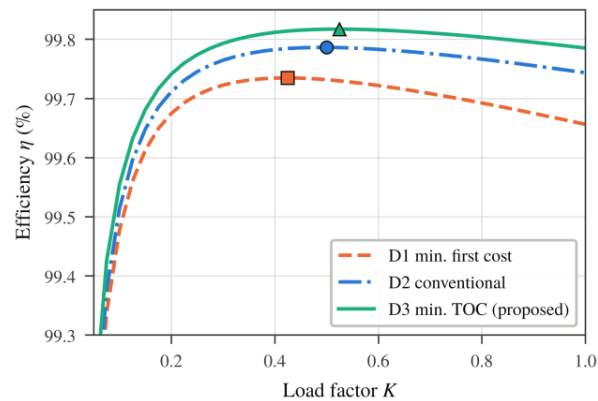


Figure 13: Efficiency Versus Load Factor of Designs D1-D3 Including Harmonic and Tap-Tracking Effects; Markers Indicate the Maximum Efficiency

5.4. Sensitivity to Economic Assumptions

Because the optimum depends on the value placed on losses, the design was re-optimized for different energy values, discount rates, lifetimes and copper prices (Fig. 14 and Table VII). As C_e increases from 0.03 to 0.12 USD/kWh, the optimal current density falls from 2.68 to 2.13 A/mm², the laser-scribed grade becomes optimal from 0.08 USD/kWh onwards, and the TOC saving relative to D2 grows from 1.5 %

to 7.2 % (from 4.7 % to 17.6 % relative to D1). At low energy values the optimal flux density returns to the over-excitation limit and the design moves towards the minimum-first-cost region. The saving is positive in every scenario, which shows that the conclusion does not hinge on the base-case assumptions; it is most sensitive to the energy value and the discount rate and least sensitive to the assumed lifetime.

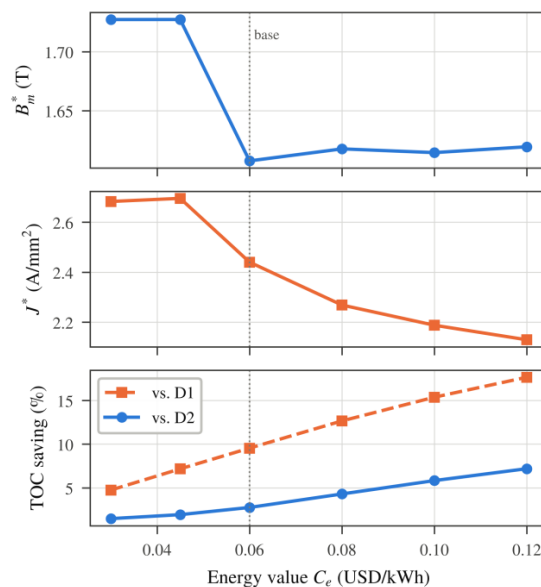


Figure 14: Sensitivity of the TOC-Optimal Design to the Value of Energy: Optimal (a) Flux Density, (b) Current Density, and (c) TOC Saving Relative to D1 and D2

Scenario	$B_{m,opt}$	J_{opt}	t_{opt}	s_g	P_{Σ}	vs D2	vs D1
$C_e = 0.03$	1.727	2.68	1.43	1	1200	1.48	4.74
$C_e = 0.045$	1.727	2.70	1.35	1/S	1171	1.93	7.17
Base case	1.607	2.44	1.30	1/S	1077	2.75	9.54
$C_e = 0.08$	1.618	2.27	1.22	2/S	1010	4.30	12.65
$C_e = 0.10$	1.614	2.19	1.15	2/S	993	5.83	15.36
$C_e = 0.12$	1.619	2.13	1.11	2/S	980	7.19	17.64
$r = 4\%$	1.618	2.27	1.22	2/S	1011	4.21	12.49
$r = 8\%$	1.717	2.65	1.34	1/S	1156	2.04	7.56
$n = 30$ y	1.649	2.52	1.31	1/S	1107	2.42	8.72
$n = 50$ y	1.596	2.41	1.29	1/S	1067	2.96	9.99
Cu price +25 %	1.620	2.61	1.39	1/S	1112	2.23	8.77
Cu price -25 %	1.597	2.25	1.20	1/S	1036	3.56	10.57

C_e in USD/kWh; $P_{\Sigma} = P_0 + P_{LL}$ in kW; S: tank shunts; savings in %.

Table 7: Re-Optimized Design and Toc Saving for Different Economic Scenarios

5.5. Operational Optimization

Table VIII lists the annual transformer losses of D3 for different firing-angle set-points. Lowering α_{ref} from 15° to 12.5° reduces the rated valve voltage by 1.1 %, the annual no-load energy by 3.2 % and the reactive power per unit at rated load from 302 to 290 Mvar, while the larger overlap slightly reduces the harmonic losses. Over the year this set-point coordination saves 110.5 MWh per unit (2.65 GWh per

station) and reduces the mean reactive demand of the station by about 206 Mvar. The lower limit of α_{ref} must preserve a sufficient margin above the minimum firing angle and a dead band of at least one tap step (about 2.7° at 15° and 3.2° at 12.5° for a 1.25 % step), so 12.5° is used as a practical value; its suitability has to be confirmed by the converter-control studies of the specific project.

$\alpha_{ref} (^\circ)$	U_v/U_{vN}	$\mu_N (^\circ)$	E_{NL}	E_{LL}	$E_{NL}+E_{LL}$	Q_N
10.0	0.9808	30.5	2097	2962	5059	280
12.5	0.9894	28.6	2152	2994	5146	290
15.0	1.0000	26.8	2222	3029	5251	302
17.5	1.0128	25.1	2309	3066	5375	316
20.0	1.0279	23.5	2414	3106	5519	333

Energies in MWh/yr per unit (75°C); U_v/U_{vN} and μ_N at rated load; Q_N : reactive power per unit at rated load (Mvar).

Table 8: Effect of the Firing-Angle Set-Point on Annual Losses of D3

The effect of LOCC is illustrated for an autumn week in Fig. 15. Instead of running all three duty coolers of D3 continuously, the controller uses one or two coolers at part load and three only when the load or ambient temperature requires it. Over the year, LOCC reduces the auxiliary energy per unit from 283 to 198 MWh (−30.3 %). The warmer windings raise the $I^2 R$ loss by 60.2 MWh but reduce the eddy loss by 13.8 MWh because of (47), so the net saving is 39.4 MWh per unit

(0.95 GWh per station). The maximum hot-spot temperature increases from 88.6°C to 90.1°C , still below the 98°C limit, and the annual equivalent ageing rises from 8.8 h to 40.0 h, which is negligible against the 8760 h of normal ageing at the reference temperature. These results are consistent in direction with the measured efficiency benefit of cooling management reported for power transformers [29,30].

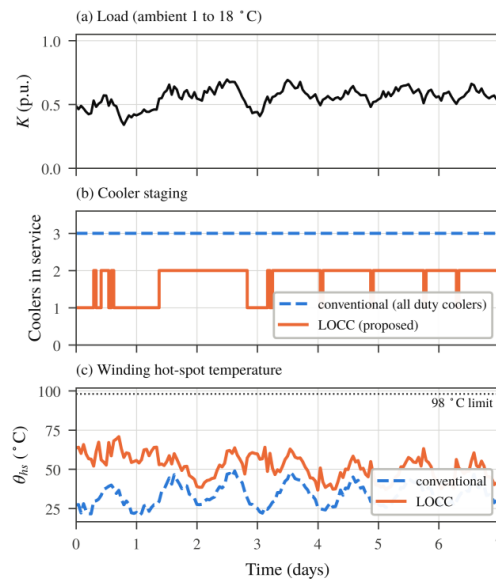


Figure 15: Operation During One Autumn Week (Design D3, $\alpha_{ref} = 12.5^\circ$): (a) Load Factor, (b) Coolers in Service Under Conventional Control and LOCC, (c) Winding Hot-Spot Temperature

5.6. Station-Level Energy, Operating Cost and Emissions

Table IX and Fig. 16 summarize the annual results for one sending-end station with 24 units, obtained from the hourly simulation with temperature-corrected losses. The conventional reference S0 (design D2, $\alpha_{ref} = 15^\circ$, all duty coolers) loses 152.0 GWh per year in its converter transformers, i.e., 0.230 % of the 66 TWh transmitted. Replacing D2 by D3 (S1) saves 16.8 %, and adding set-point coordination (S2) and LOCC (S3) raises the total saving to 29.1 GWh per year (19.2 %). At 0.06 USD/kWh this is worth

USD 1.75 million per year and avoids about 16.6 kt of CO₂ per year with the assumed emission factor. Compared with a minimum-first-cost station (D1), the saving is 75.5 GWh, or USD 4.53 million, per year; the D1 units also reach a hot-spot temperature of 103.5 °C at summer peak load, above the 98 °C reference, with 33.6 h of equivalent ageing per year. Because the receiving-end station contains a further 24 units of similar rating, the benefit for the complete link is approximately twice these values if the same approach is applied there.

Strategy	E (GWh)	Share (%)	Cost (MUSD)	ΔCO_2 (kt)	$\theta_{hs,max}$ (°C)	Ageing (h)
D1, conventional	198.3	0.301	11.90	-26.4	103.5	33.6
S0: D2, conventional	152.0	0.230	9.12	0.0	91.4	11.2
S1: D3	126.5	0.192	7.59	+14.5	89.4	9.6
S2: S1 + α /OLTC	123.8	0.188	7.43	+16.1	88.6	8.8
S3: S2 + LOCC	122.9	0.186	7.37	+16.6	90.1	40.0

Share of the 66 TWh transmitted per year; cost at 0.06 USD/kWh; ΔCO_2 relative to S0 (positive = reduction).

Table 9: Annual Station-Level Results (24 Units, Sending End)

Share of the 66 TWh transmitted per year; cost at 0.06 USD/kWh; ΔCO_2 relative to S0 (positive = reduction).

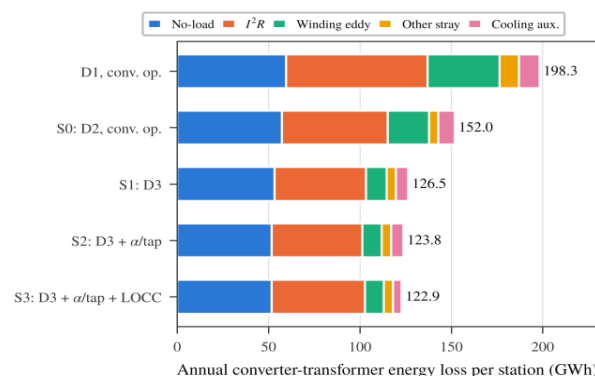


Figure 16: Annual Converter-Transformer Energy Loss of One Station (24 units) By Loss Component for the Minimum-First-Cost Design and Strategies S0-S3

5.7. Practical Considerations and Limitations

The results rest on an analytical model whose loss and thermal coefficients (k_{end} , c_{SE} , g_{w0} , the space factors) have been set to representative values rather than fitted to a specific manufacturer's design; three-dimensional finite-element analysis of the stray field and factory test data are needed to calibrate them for a real tender [26-28]. The insulation design (gaps and end distances) has been held fixed, although it interacts with the leakage geometry, and acoustic noise, DC-bias withstand and short-circuit forces have not been included as constraints. The load and ambient profiles are synthetic, so the absolute annual figures should be recomputed with measured operating data. In operation, the OLTC is discrete and the minimum firing-angle margin, commutation-failure risk at the inverter and valve damping losses limit the usable α_{ref} ; LOCC must be implemented with hysteresis and minimum run times to avoid excessive switching of pumps and fans. None of these limitations changes the direction of the conclusions, which hold over the full range of economic assumptions examined.

6. Conclusion

A three-layer framework has been developed to optimize the efficiency, minimize the transmission losses and reduce the operating cost of 1100 kV converter transformers in an LCC-UHVDC link. The analytical model links converter operation, electromagnetic design, harmonic losses, thermal limits, transport constraints and lifetime economics in a form that is fast enough for global optimization. For a ± 1100 kV, 12 GW link with 611.8 MVA units, the main findings are as follows.

- The TOC-optimal design (1.607 T, 2.44 A/mm², 1.3 mm CTC strands, domain-refined steel, tank shunts) reduces the rated losses to 1077 kW, 16.3 % below a conventional design and 37.6 % below the minimum-first-cost design, and raises full-load efficiency to 99.79 %; the TOC falls by 2.75 % and 9.54 %, respectively, worth USD 12.4 million and USD 46.1 million per station.
- Converter-transformer harmonic factors depend strongly on load. Evaluating the annual load loss with the rated spectrum and K^2 scaling underestimates it by up to 7.1 %, while OLTC tracking lowers the no-load energy by about 12 %; profile-based capitalization factors (39)–(40) are therefore recommended.
- The CTC strand thickness is a key loss-reduction variable for converter transformers, with an optimum near 1.3 mm in this case; the optimal design uses 594 t of the 600-t transport-mass allowance, so this limit is close to becoming active for higher energy values.
- Firing-angle/OLTC set-point coordination and loss-optimal cooling control save a further 2.65 GWh and 0.95 GWh per station and year, respectively, at negligible ageing cost; together with the optimal design they reduce annual converter-transformer losses by 19.2 % (29.1 GWh, USD 1.75 million and 16.6 kt CO₂ per station and year).

Future work will calibrate the stray-loss and thermal coefficients with finite-element models and factory test data,

extend the design space to the insulation system and to noise and short-circuit constraints, and apply the framework to the receiving-end units and to measured operating data.

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